FEBRUARY 1957

THE WELDING JOURNAL

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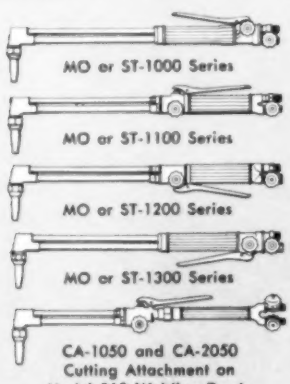

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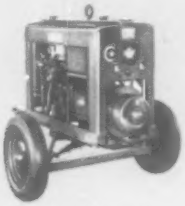

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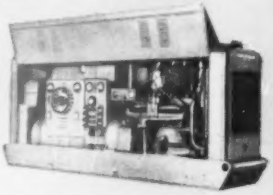

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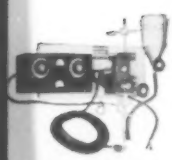

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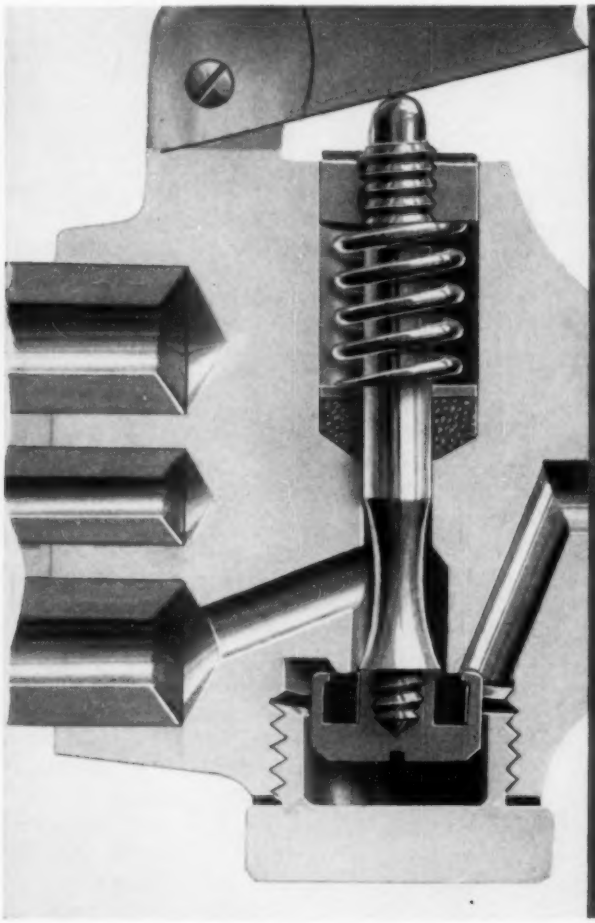

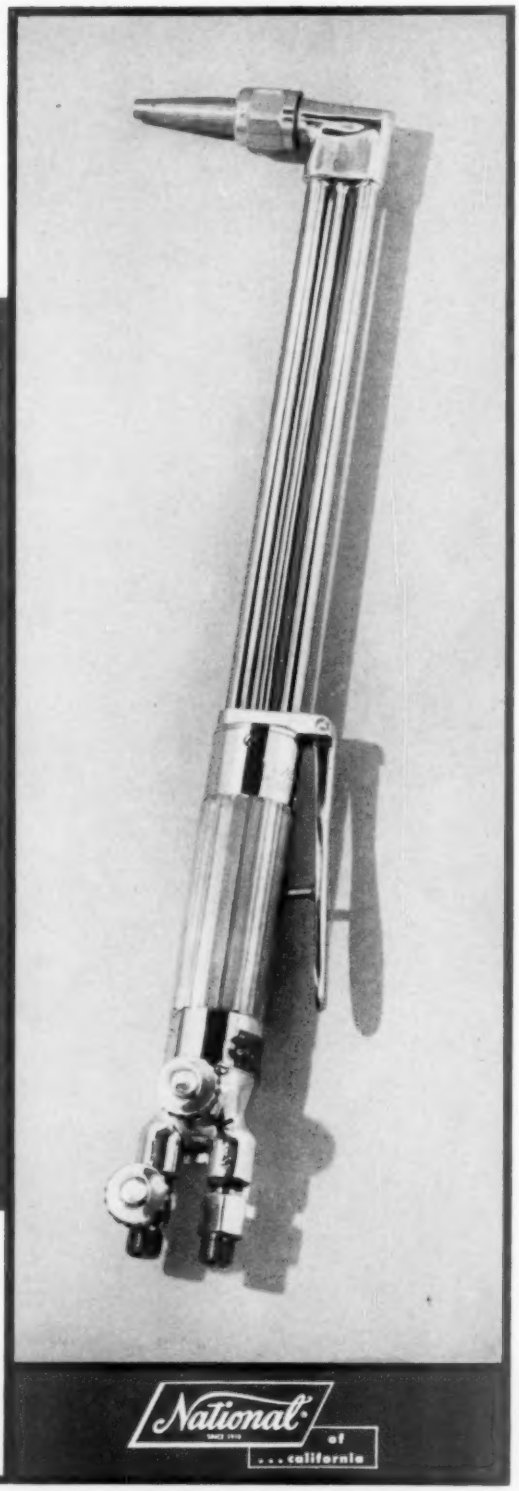

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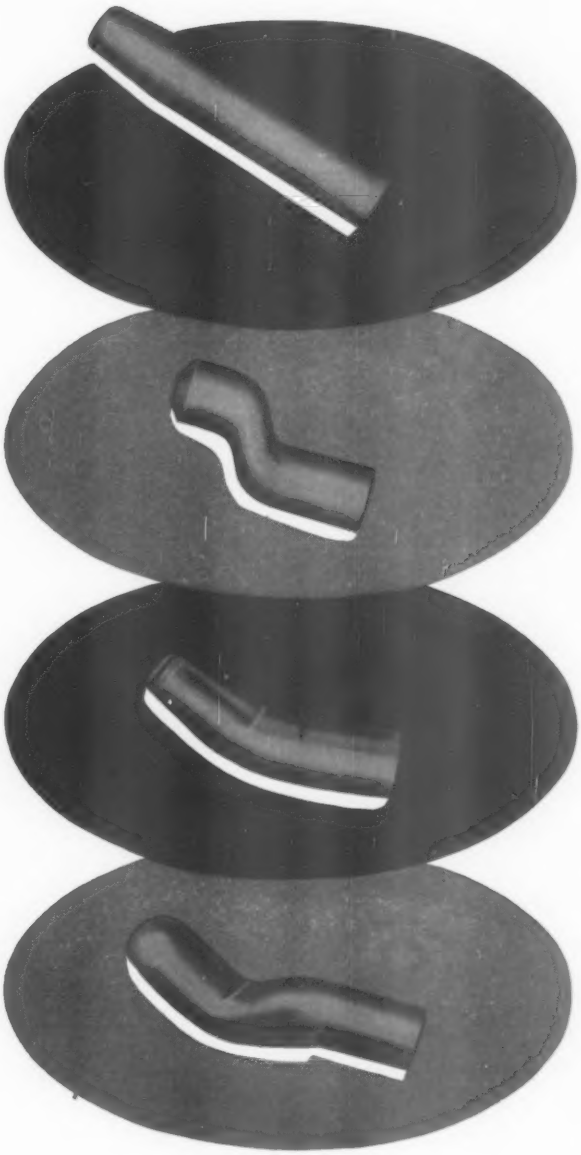

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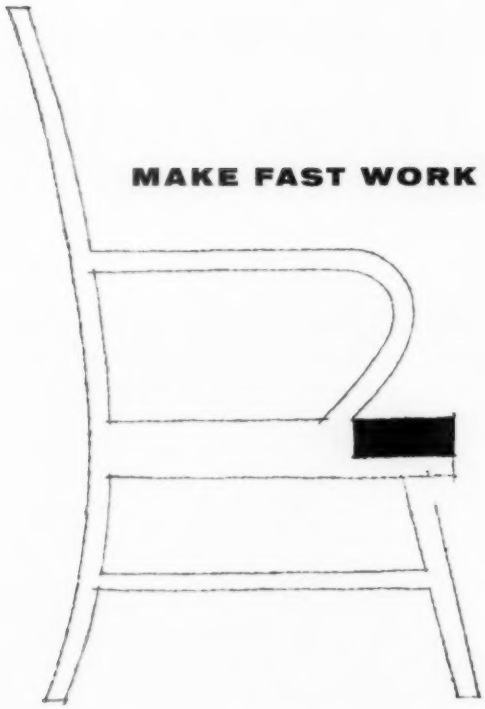

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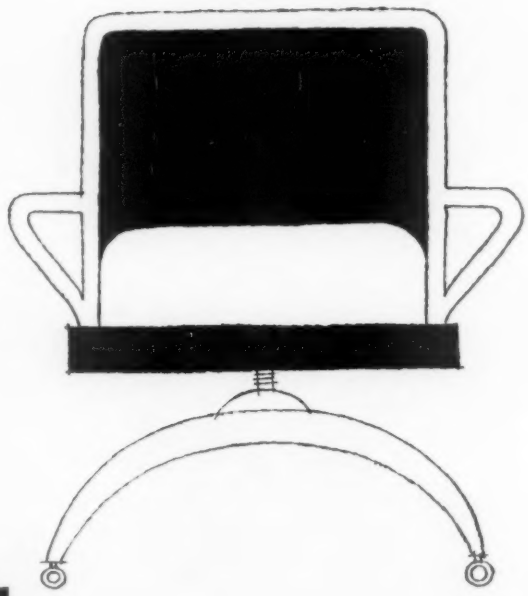

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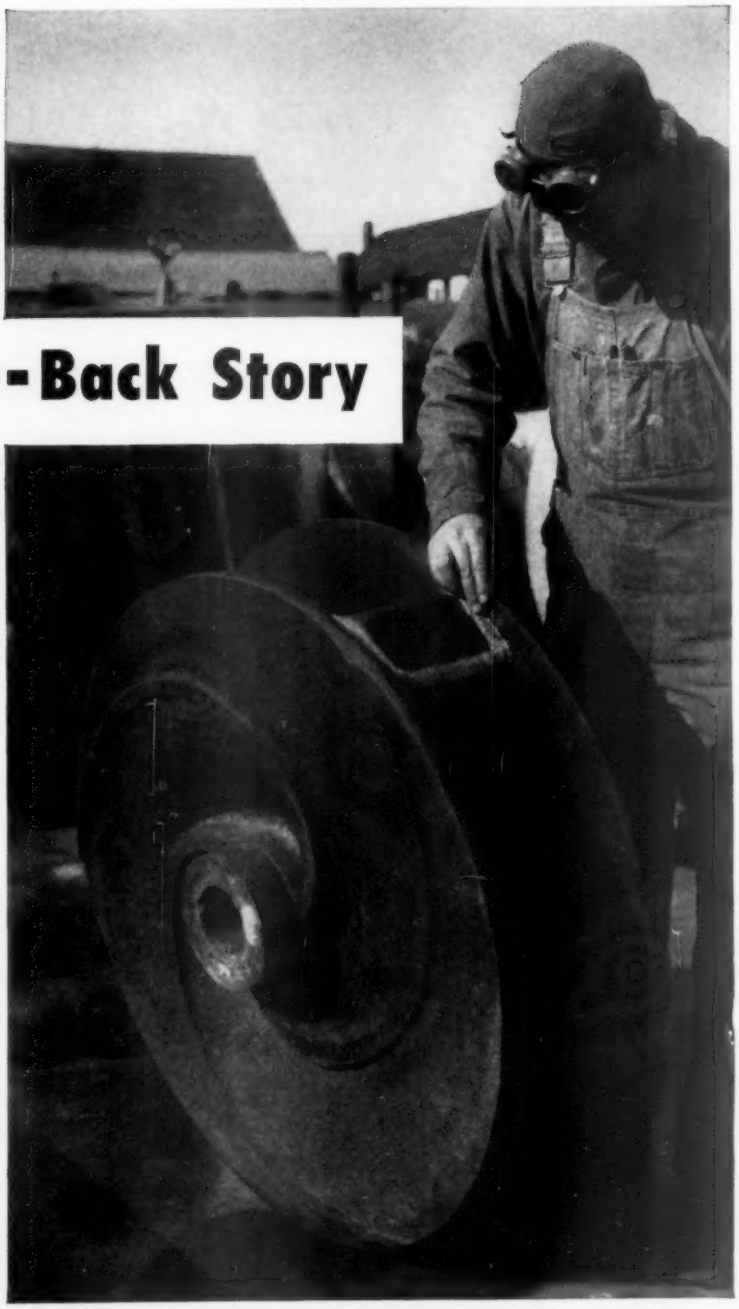

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EDITORIAL

WELDING IN SHIPBUILDING

A reputation for dependability must be earned over a long period of time. For a person, a product or a process, such confidence is gained only after long and serious scrutiny by many people and their acceptance is worth the effort.

Today, there is an increasing demand for large tankers. These tankers will carry over five times as much oil as the T-2's which were built during the World War II era. They will be welded.

They will be welded, not because someone has been "sold a bill of goods" but because those who invest their money are convinced that only through welding can they get the dependability and economy they need.

This confidence has been built up within the industrial life-span of most of the men who will read this editorial and they will recall the difficulty, the worry and the heartbreaking efforts which built it.

Early in the 20's, welding made its first feeble assaults on riveting in shipbuilding. Its progress was slow, steady and confident, until the outbreak of World War II when its reputation was laid on the line in the interest of national preservation. During the winter of 1943–1944, several ship failures occurred, causing many to wonder if welded ships would continue to be built when the need for fast delivery was passed.

History but repeated itself. The challenge was met head-on by a concentrated cooperative research program the like of which the world has never seen. Shipbuilders, ship owners, classification societies, government agencies under the coordination of the Ship Structures Committee, armed forces, colleges, research centers and technical societies pooled their facilities, their know-how and their money in a search for knowledge that was unprecedented. In all of this, the guidance provided by the AMERICAN WELDING SOCIETY and by the Welding Research Council was paramount.

We have not yet learned all the answers but the search continues. The results of that program, both of research and education, have reestablished confidence in the welded ship and have built up the acceptance of welding to a far greater degree than ever before. Just as there was work enough for all, there is now glory enough for all.

This too has been worth the effort.

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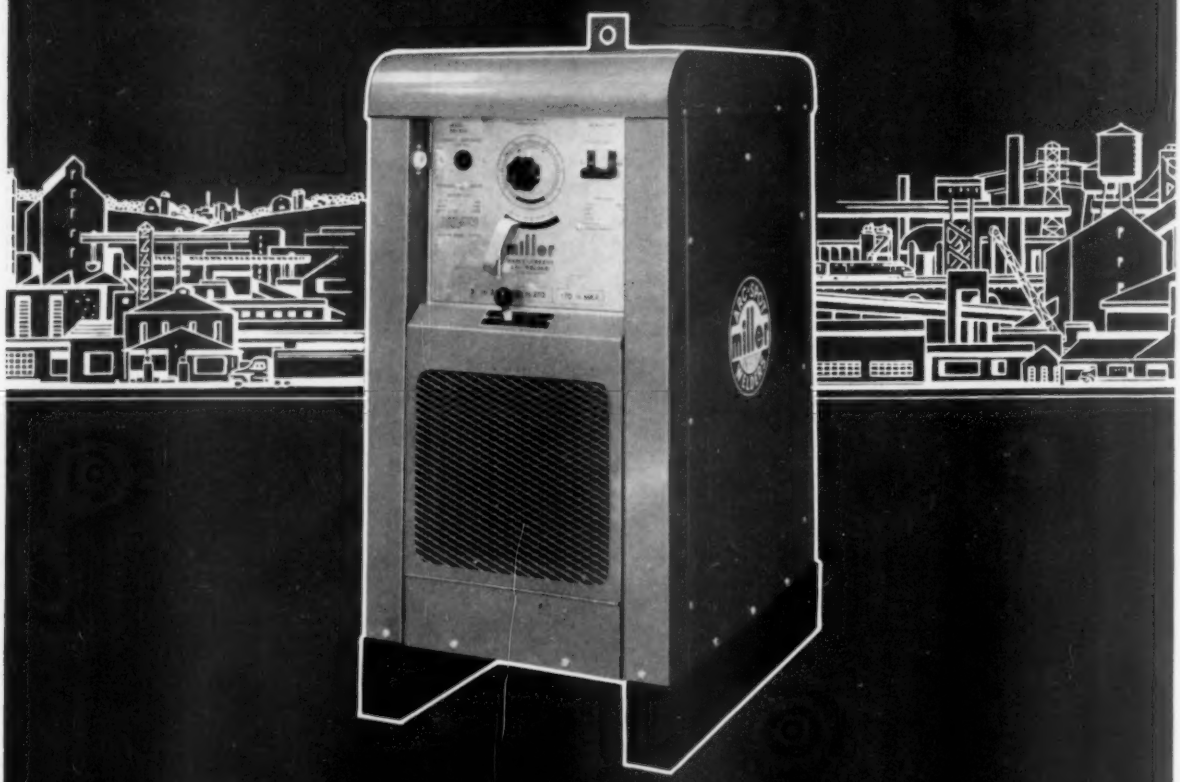

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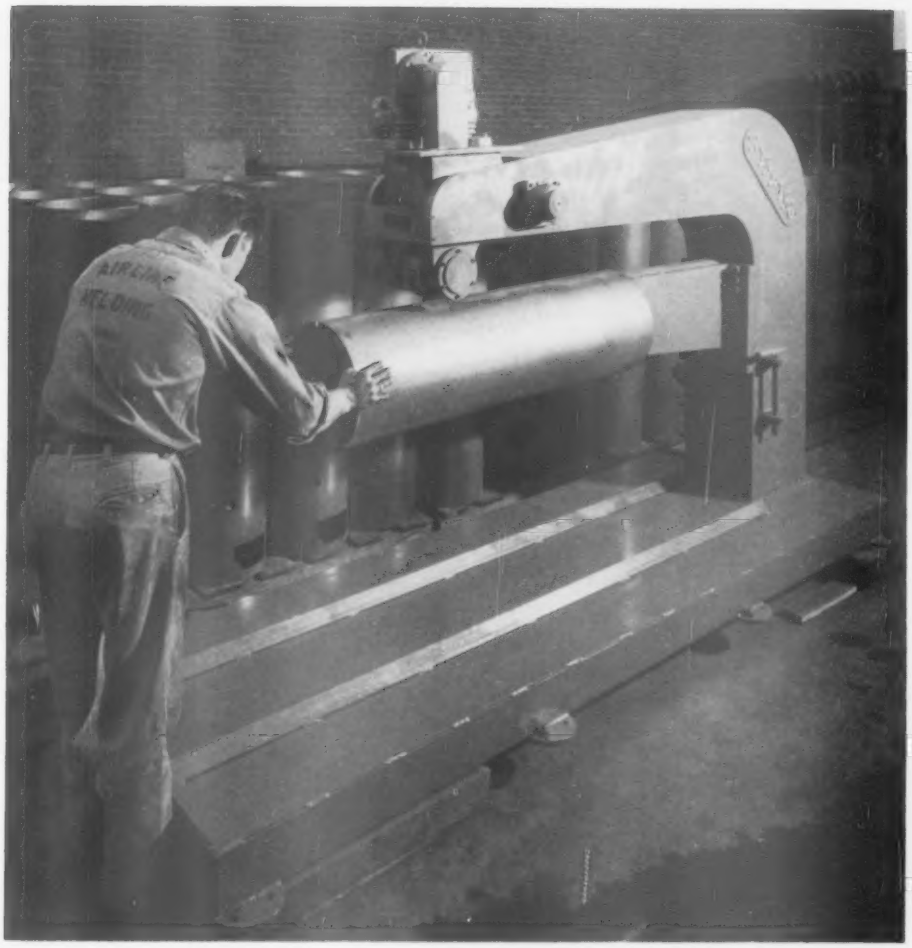


Fig. 1 Typical roll-planishing operation. Heavy-duty machine shown here is capable of developing 20,000 lb of pressure between the pressure rolls

ROLL PLANISHING IMPROVES WELD-JOINT EFFICIENCY AND QUALITY

BY H. L. MEREDITH AND B. R. RUSSELL

Increased yield and tensile strengths, decreased elongation and improved fatigue strength listed among the effects of roll planishing

H. L. Meredith and **B. R. Russell** are associated with Airline Welding and Engineering Co., Hawthorne, Calif.

Presented at 1956 AWS National Fall Meeting in Cleveland, Ohio, October 8 to 12.

Introduction

Day after day, we hear and read how the inert-gas nonconsumable and consumable electrode welding processes are being used more and more with gratifying results. But, what we do not read or seldom hear, is the painstaking efforts that are generally expended in getting the proper weld settings and procedures that are necessary to attain gratifying weld results. Although the processes themselves are designed from the results of scientific studies, it still takes a skilled and practiced

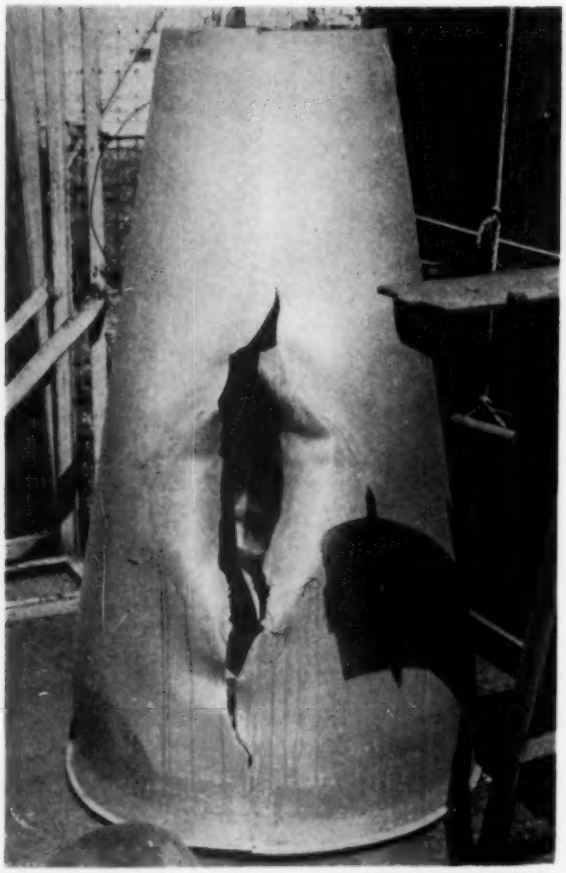


Fig. 2 Automatically inert-arc welded missile nose section made from 6061-S aluminum. Weld was ground flush before bulge forming. Failure occurred in weld during last stages of forming

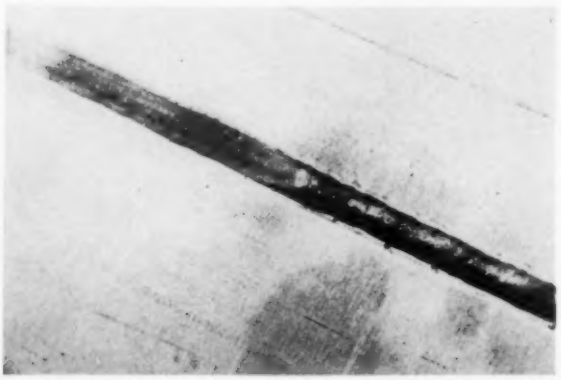


Fig. 3 Submerged-arc weld in 12-gage mild steel. The weld has been partially planished to show flattening and smoothening action of pressure rolls

operator to handle the torch, whether it be manual, semiautomatic or full automatic.

To produce *top quality* weld joints, whether it be the welding of 17-7 PH, unalloyed titanium, aluminum or the weldable 18-8 stainless steels, the welder needs a good understanding of weld variables, of which there are basically seven, and above all, he needs *good welding equipment.*

When a welding man finally realizes that he has all these things at his finger tips he concentrates on one more accomplishment: "How can I make the weld joint stronger than the as-welded condition without resorting to heat treating?"

Factors Causing Loss in Weld-Joint Strength

Loss in weld-joint strength is attributable to four main factors. The foremost factor is loss in tensile strength due to the weld being a cast metal. The second factor is loss in ductility. The third is the notch effect caused by abrupt change in thickness at the weld edge. The fourth factor is attributable to the enlarged grain structure at the heat-affected zone; the transition from cast structure to lamellar structure is not uniform.

Effect of Cold Working Sheet Metal

To gain tensile and yield strength in the manufacturing of sheet metal, it is necessary to either heat treat or cold work the hot-rolled structure. In the case of wrought type materials (metals which do not respond to heat treatment), cold working raises the ultimate tensile and yield strength and improves the ductility if not overly worked.

Some metals which respond to heat treatment are also susceptible to cold working. For instance, the improvement in the mechanical properties of 2024-S aluminum, particularly the yield strength, is much greater if the metal is cold worked prior to artificial aging. The small amount of stretching (about 1%) that is necessary to remove the buckles and distortion after quenching is sufficient to cause an increase in yield double that developed by sheet that has not been cold worked.

Roll Planishing

The objectives of roll planishing welds is manifold. The principle objective is to improve the mechanical properties of the weld joint. The first use of roll planishing at our factory was on the aluminum nose cone section of the corporal missile. Figure 2 shows a completed nose section that failed during proof testing. This type of failure occurred on innumerable parts and soon had production stopped due to such high scrap percentages. In an effort to eliminate these failures, the longitudinal welds were hammer planished to cold work the weld. Pressure tests showed no failures. The welds were producing 100% joint efficiencies. Because hammer planishing required skilled metalsmiths and was too slow for high production, a machine was needed.

Figure 1 shows a heavy duty 10-ton roll-planishing machine built for an aircraft company. The machine was proof tested by planishing square butt welds in 12-gage mild steel. The welds were made by the submerged-arc process. Figure 3 illustrates the flattening action of the planishing rolls. As shown in Figure 4, two rollers, diametrically opposed, exert pressure at a small contact area. The top roller is the driver and the lower is the follower. Extremely high or very low

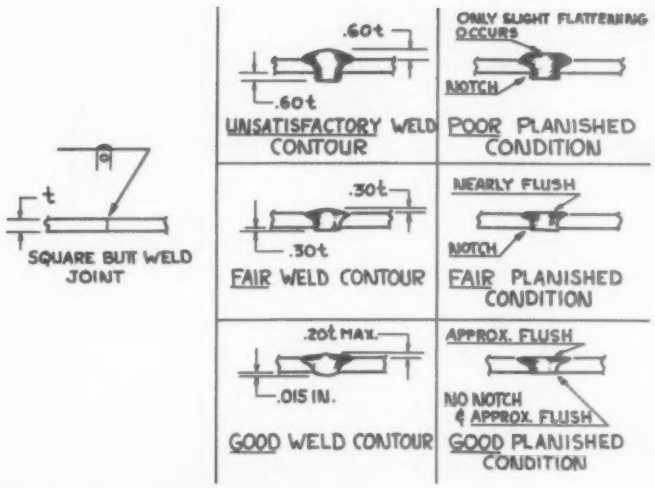


Fig. 4 Weld contour for roll planishing

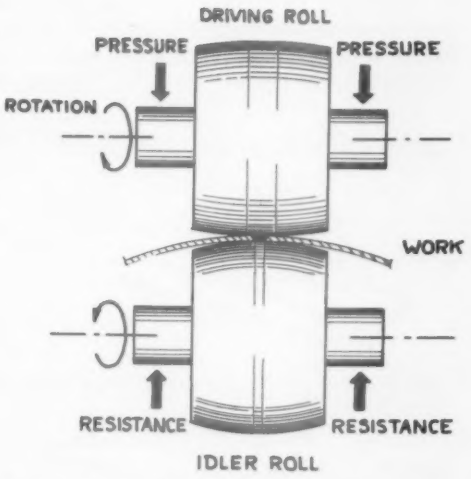


Fig. 5 Roll-planishing process

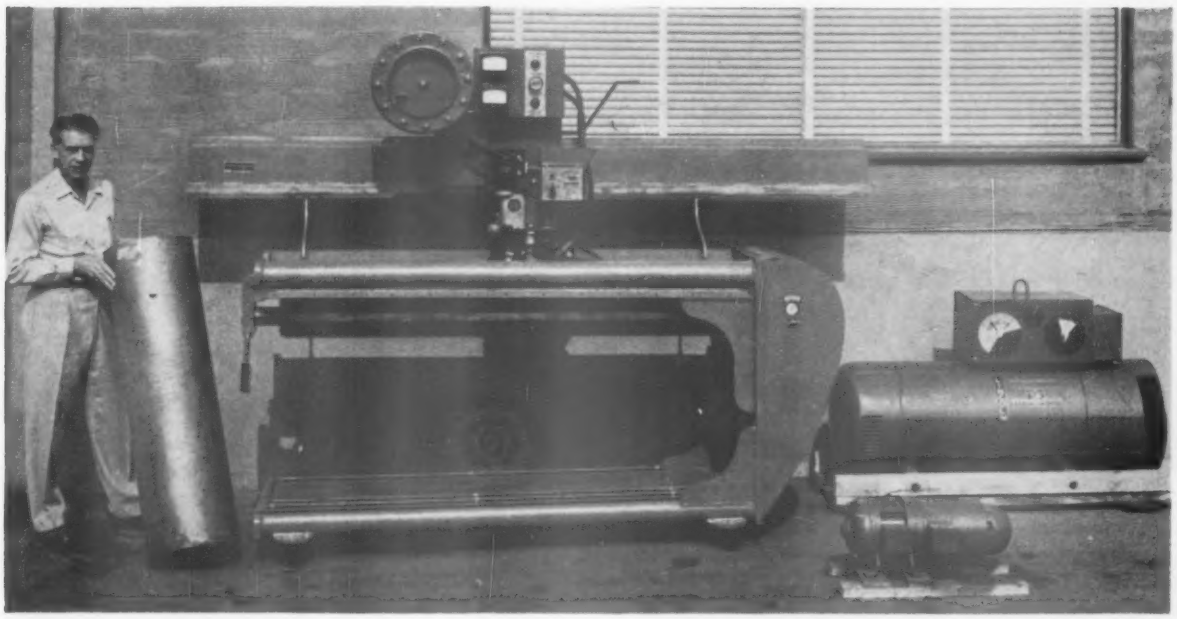


Fig. 6 Longitudinal welding machine used for submerged-arc welding of square butt joints

loads may be exerted upon contact. The magnitude of the output load can be adjusted pneumatically, from 0 to 20,000 lb. Hence, if a weld $^1/_4$ in. wide is to be roll planished, a compressive force as high as 416,000 psi may be attained if the driver makes $^1/_8$-in. wide contact (using $6^1/_2$ tons force).

Planish Procedure

The correct application of the roll-planishing process to a given metal is dependent upon six variables. These six variables are:

1. Base metal thickness.
2. Weld width and contour.
3. Weld-reinforcement height.
4. Roll-planished pressure.
5. Roll-planishing surface speed.
6. Surface finish.

Of these six variables, the weld-reinforcement height and weld width are the most important and must be rigidly controlled during welding. For a given metal thickness, the weld width should be held within a range of 3 to 5 times the base metal thickness. This ratio is in agreement with good joint design practices and also represents an economical balance. This thumb rule applies to all metals; aluminum and magnesium being the hardest to control. Where filler metal is used, the height of weld reinforcement for both the weld-bead crest and root should be held approximately flush. Figure 4, illustrates the various effects

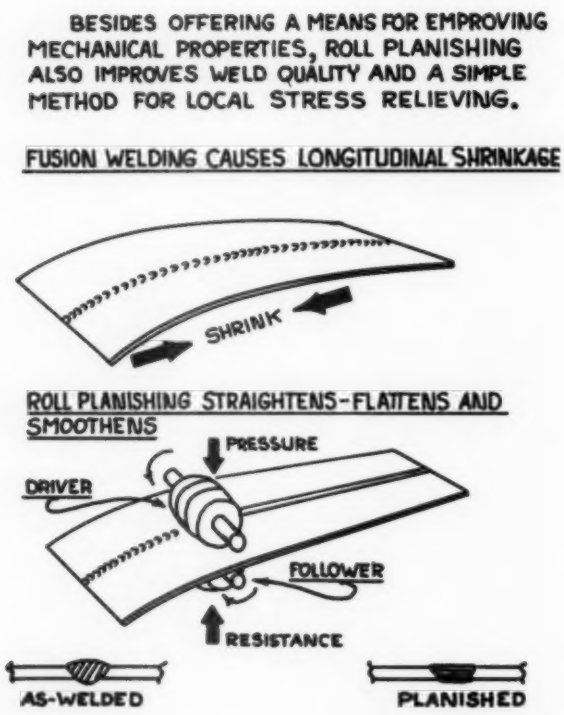


Fig. 7 Basic planishing concepts

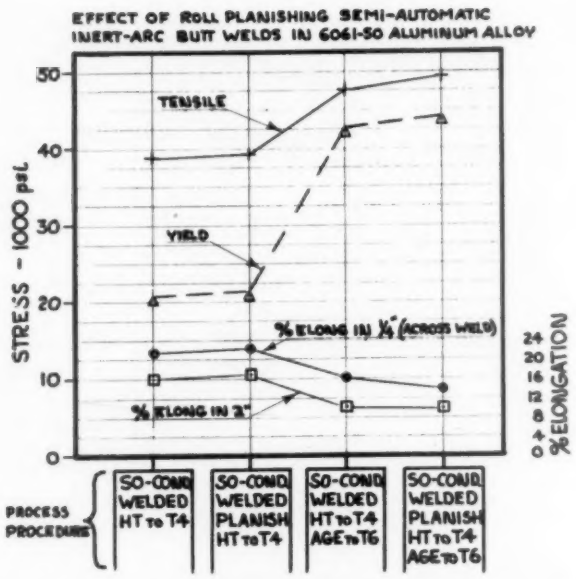

EFFECT OF ROLL PLANISHING SEMI-AUTOMATIC INERT-ARC BUTT WELDS IN 6061-SO ALUMINUM ALLOY
STRESS - 1000 psi.
TENSILE
YIELD
% ELONG IN 1/4" (ACROSS WELD)
% ELONG IN 2"
% ELONGATION
PROCESS PROCEDURE
SO-COND WELDED HT TO T4
SO-COND WELDED PLANISH HT TO T4
SO-COND WELDED HT TO T4 AGE TO T6
SO-COND WELDED PLANISH HT TO T4 AGE TO T6

Fig. 8 Process vs. mechanical properties comparison graph

that roll planishing has on different weld contour proportions.

This means that less filler wire is required. Consequently, it forces another manufacturing savings.

Where no filler wire is used, and the weld joint is merely fused together, the weld-bead height above the base metal surface is generally zero or concave. Therefore, it is necessary to control the concavity, in metal thicknesses of 0.020 to 0.070 in. This means that where concavity is obtained it should never be greater than 0.010 in. deep; if the welding procedure is changed so that convexity is obtained, then the reinforcement should not be greater than .20 t above the base-metal surface.

Effect of Weld Size

Other objectives of roll planishing are to relieve weld shrinkage strain and also flatten and smoothen weld surfaces. Consequently, it is necessary to control cross-sectional area of the weld. If the weld-bead crest height is greater than the base-metal thickness, then it is easy to see that when the excess weld metal is squashed back into the weld area, the adjacent base metal must move. This causes excessive distortion and canning. The basic concept is very simple. Just enough weld reinforcement is required so that it may be compressed enough to refine the surface grain structure, counteract shrinkage strain and also be flattened.

Roll-Planishing Pressure

The amount of cold working that a cast-weld structure may take is dependent upon many factors. Roll planishing is limited only by the characteristics of the weld-joint configuration and metallurgical limitations of the base metal. For instance, a square butt weld in 0.040-in. thick 2S-O aluminum may be planished several times at 40-psi gage pressure, but a square butt weld in 0.040-in. thick Ti75 titanium may only be planished once.

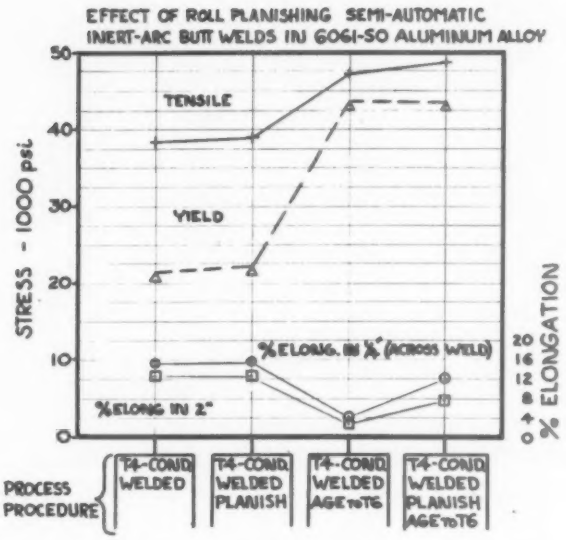
Weld Shrinkage

It is a well known fact that long welds made in square butt joints will shrink transversely and longitudinally (see Fig. 7). The rate of longitudinal shrinkage is uniform when welding is done in a longitudinal fixture. However, the rate of transverse shrinkage increases as the length of weld increases.

Recent inert-arc butt-weld tests made by a very reliable source showed that the average transverse shrinkage in 0.027 in. stainless steel, Type 301 extra hard, was on the order of 0.010 to 0.012 in. in the as-welded condition.

By roll planishing the welds at 40 psi on a standard machine, over-all shrinkage was partially reduced. Micrometer measurements revealed that the average final shrinkage was reduced to 0.008 in.

To clarify this information, further tests were made. The tests consisted of welding six weld panels together. Each panel was sheared to a nominal width of $4^{1}/_{16}$ in. wide by 18 in. long. The material was Type 301 full hard, 0.025 in. thick. After milling each edge square, the exact width of each specimen was measured in thousandths of an inch. The average width was 4.0098. When each panel was laid side by side the total width came to 24.059 in. Five welds were made by inert-gas tungsten-arc fusing the square butt joints together—no filler wire was added. Welding was


EFFECT OF ROLL PLANISHING SEMI-AUTOMATIC INERT-ARC BUTT WELDS IN 6061-SO ALUMINUM ALLOY
STRESS - 1000 psi
% ELONGATION
TENSILE
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% ELONG. IN 2" (ACROSS WELD)
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PROCESS PROCEDURE
T4-COND. WELDED
T4-COND. WELDED PLANISH
T4-COND. WELDED AGE TO T6
T4-COND. WELDED PLANISH AGE TO T6

Fig. 9 Process vs. mechanical properties comparison graph

done in a production welding fixture incorporating the chill-shunt principle (see Fig. 6). The final dimension across the starting end of the welded specimen was 24.007 in. This gave an average shrinkage of 0.0104 in. per weld. The final dimension across the finishing end of the specimen was 23.989. This gave a total shrinkage of 0.070 or an individual shrinkage of 0.014 in. per weld. So, in 18 in. of weld length, the difference in as-welded shrinkage between starting end and the finishing end is 0.0036 in.

The welds were roll planished after shrinkage measuration. A gage pressure of 40 psi was used with a $^3/_4$-in. width flat area on the top roller face and a 6-in. radius contour on the bottom roller. This combination was necessary because the weld-bead crest was slightly concave. Each weld was given a single planishing pass. The final total measurement across the starting end was 24.028 in. This indicated that planishing caused an average expansion of 0.004 in. at the starting end. The final total measurement across the weld finishing end was 24.012 in. This showed two things: (1) the average expansion was on the order of 0.0045 in. which indicated uniform planishing; and (2) a slight cumulative increase in expansion can be expected with straight-through planishing.

Table I—Mechanical Properties of Inert-Gas Tungsten-Arc Welded Square Butt Joints

Material: AISI 301 F.H.—0.040-in. sheet, 1-in. gage width

Room temp. tests, spec. condition	*Average ultimate psi*	*% Elongation in 2 in*
As-welded	130,000	4.6
Roll planish—40 psi	144,000	3.8

Material: 6061-T4—0.040-in. sheet, $^3/_4$-in. gage width

Room temp. tests, spec. condition	*Average ultimate psi*	*% Elongation in 2 in.*
Base metal	37,000	19.0
Ax-welded	32,000	11.0
Roll planish—40 psi	38,000	17.0

Material: 2024-T3—0.070-in. sheet, $^3/_4$-in. gage width

Room temp. tests, spec. condition	*Average ultimate psi*	*Average yield strength, psi*
As-welded, flushed both side	50,500	41,000
Roll planish—40 psi, flushed both sides	52,000	45,000
As-welded, root flushed only	53,600	42,700
Roll planish-40 psi, root flushed only	52,000	43,000

Tensile Strength of Planished Welds

Since roll planishing is relatively new to the light-metal fabrication field, the most obvious question to arise, is: "How does planishing effect weld strength?"

Roll planishing has been found to (1) increase the ultimate tensile strength, (2) raise the yield strength, (3) slightly decrease the elongation and (4) improve the fatigue strength.

Room-temperature tensile tests of semiautomatic inert-gas tungsten-arc welds in Type 301 F.H. stainless steel, 61S-T4 and 24ST-T3 aluminum alloys have been very encouraging.

The tests were made to compare the as-welded tensile strength with the roll-planished tensile strength.

The results of the tensile tests are given in Table 1.

The effect of roll planishing inert-arc butt welds in 6061-SO Aluminum, before heat treating, is illustrated by the graphs in Figs. 8 and 9. These processing graphs compare the mechanical properties obtained from as-welded and heat-treated test specimens with the mechanical properties of welded, planished and heat-treated test specimens.

Starting from the left column and proceeding to the right, the graphs show that planishing helps to improve the mechanical properties. Although not represented in the graph, one of the most significant results was that planishing improved weld-joint efficiency. Weld-joint failures were more predominant with the first and third processing procedure than with the second and fourth processing procedure.

The most significant results of these tests are shown in Fig. 9. The third column represents data obtained from welded specimens which were processed in the normally accepted manner. Note that the average percent of elongation is approximately 2.7% for the base metal and 4% for the weld joint. This is very low, and represents a dangerous condition, especially if notch-type stress raisers exist at the weld edge. On the other hand, note the results of the last column in Fig. 9.

These data were obtained from specimens taken from the same welded panel as used for the third column. The only difference is that the welds were planished before aging to T6 condition. The ultimate tensile strength was only slightly improved (from 47,300 to 48,600 psi). The yield strength remained fairly consistent (43,600 to 43,500 psi), but, the percent of elongation was greatly improved (from 2.7 to 7.6% for the base metal) and (4.0 to 12% for the weld metal).

Fig. 1 All-welded girder bridge without outside vertical stiffeners. Welded haunch girders span 100 and 73 ft. Bridge is located over Hoover Dam near Columbus, Ohio

WELDED JOINT DESIGN FOR ECONOMY IN STRUCTURAL FABRICATION AND ERECTION

Labor and overhead are said to constitute approximately 85 to 90% of the cost of every welded joint. To reduce this cost, joints must be designed for minimum welding and maximum welding speeds

BY OMER BLODGETT

Introduction

One of the advantages of welded structural design that has been proved by experience during the last 30 years with all types of welded structures is economy of material and time. During that period, however, other methods of structural design have also advanced and, today, to maintain its competitive advantage in the structural field, welded designs must utilize to the fullest their inherent economy. To do this, designers must not only know where stresses or forces occur in a structure, but also know how to transmit these forces through a welded connection into the supporting member in the most direct and efficient method possible. Only with this knowledge can sections be used to their best advantage and welding costs be reduced by eliminating or reducing welding time. The major portion, 85 to 90%, of the cost of a welded joint is labor and overhead, and the greatest savings, therefore, are in reducing or eliminating welding time.

The first step in improving welded design is to design for welding. It is easy to substitute welds for rivets in a design. Tables are available to convert rivets into length and size of welds. This, however, is not good welded design. It is done, unfortunately, and such designs are sometimes compared with riveted designs to the disadvantage of welding.

For good welded design, a structure must be approached from its inception as being welded without any relation to previous riveted design experience. This is not easy, since previous experience inevitably shapes our thinking and thinking in terms of riveting can unknowingly be carried over into the welded design.

For example, a recent book on structural design used the attachment of cover plates of a plate girder to illustrate a comparison between riveting and welding (see Fig. 2 center). It said that each rivet works three times in shear, whereas each weld works but once and, therefore, additional welds are required for each cover plate. While this is true, it is not a fair comparison. Welded plate girders of good design are made as shown in Fig. 2, where only two welds are used to connect a single flange plate instead of six welds connecting three flange plates.

Within the last five years, a large plate girder for a

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Presented at 1956 AWS National Spring Meeting in Buffalo, N. Y., May 7–11.

building was fabricated as shown in the top of Fig. 2. It was just a riveted design with welding applied to it. It required one web plate, four angles, two flange plates, and 12 fillet welds. It should have been fabricated as shown under good design, using only one web plate, two flange plates and four fillet welds. A similar comparison is made in Fig. 2 with a column splice.

Bending

In a loaded beam, bending moments cause bending stresses, tension and compression, to occur in the beam. These stresses are zero along the neutral axis of the beam, and it is assumed they increase linearly away from the neutral axis, being maximum at the outer fibers of the beam. To illustrate these bending *stresses* more graphically, in Fig. 3 they have been converted into *forces* by multiplying them with the corresponding areas of the sections. The flanged material has quite a bit of area and the bending stresses are high; therefore, the bending forces in the flanged material are very high. On the other hand, the bending stresses in the web material are low, and the corresponding areas are small; therefore, the bending forces in the web are very low. The drawing at the top of Fig. 3 emphasizes that bending forces are almost wholly in the flange and very little in the web.

Rules to remember for bending:

1. Bending forces (F) occur, for the most part, in the flange of the beam. Welding to transmit bending forces into a supporting member must be located in the flange to be efficient.

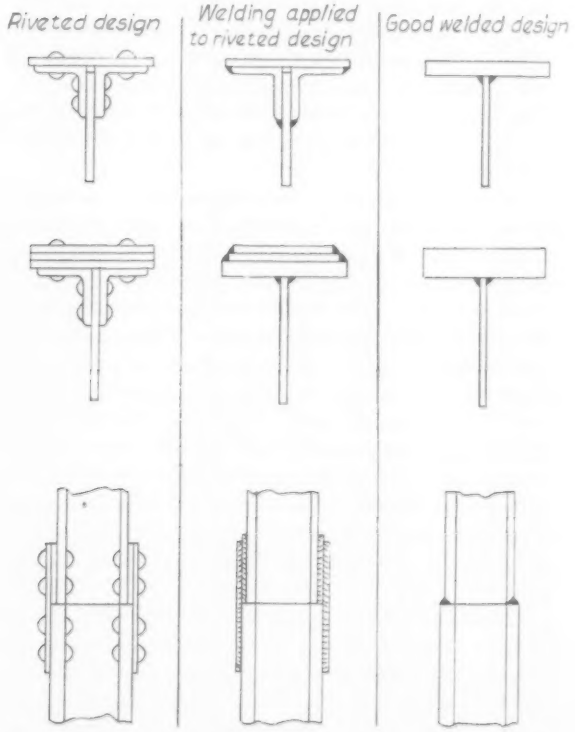


Fig. 2 Good welded design does not copy riveted design

2. Bending moment (M) usually varies along the length of the member. (Standard handbook beam diagrams cover this.)

3. Design for a required degree of flexibility, as well as for required bending-moment resistance.

In addition to having sufficient welds at the flange of the beam, it is also necessary that the supporting member be sufficiently rigid to allow the entire length of the weld to load up uniformly (see Fig. 4). If the flange of the supporting column is too flexible, the forces transmitted by the welds will load the outstanding portion of the column flange as a cantilever bead and cause it to deflect slightly. As the outer portion of this flange deflects, it will reduce the stress in the outer ends of the weld, thereby loading up the center portion of the weld in line with the web of the column. This means the center portion of the weld will be overloaded and will fail at a lower value. Unless the flange of the column is extremely rigid, it will be necessary to add

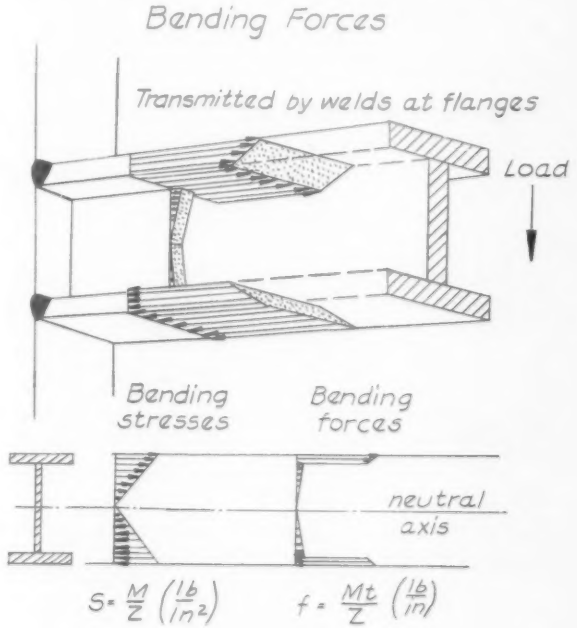


Fig. 3 Welds to resist bending moment must be located in flanges

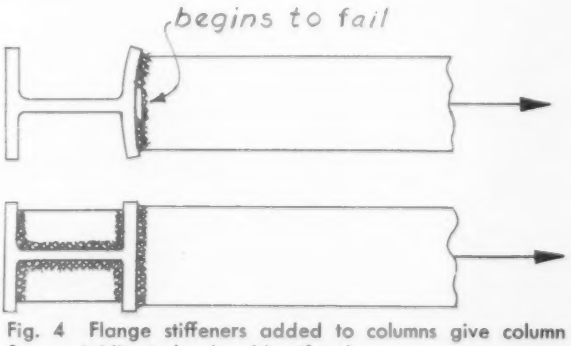


Fig. 4 Flange stiffeners added to columns give column flange rigidity to load weld uniformly

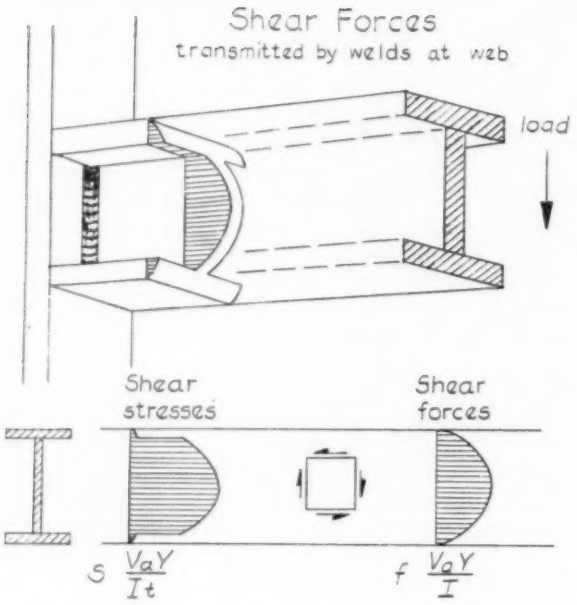


Fig. 5 Welds to resist shear must be in web

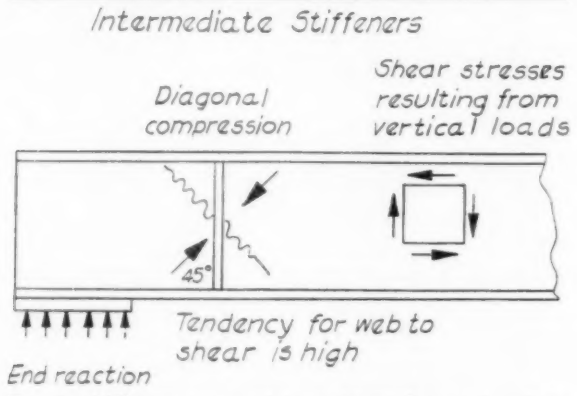


Fig. 6 Stiffeners are used to resist tendency of web to buckle from vertical shear stresses

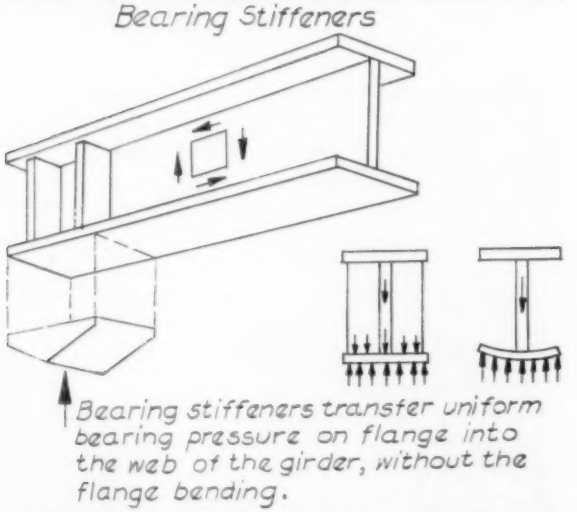

Bearing Stiffeners
Bearing stiffeners transfer uniform bearing pressure on flange into the web of the girder, without the flange bending.

Fig. 7 Stiffeners are used at points of concentrated load, such as bearing supports of plate girders

flange stiffeners to the column in line with the top and bottom flanges of the beam. This will keep the column flange from deflecting and will load the weld uniformly.

Shear

Shear forces occur as a result of a bending moment changing along the length of the beam. There are vertical shear forces, as well as horizontal shear forces. They are equal in value. Shear forces are maximum at the neutral axis of the beam and become zero at the outer fibers. Shear stresses are obtained by dividing these shear forces by the corresponding areas of the material. The top drawing of Figure 5 emphasizes that shear forces are almost wholly in the web.

Rules to remember for shear:

1. Shear forces (F) occur, for the most part, in the web of the beam. Welding to transmit shear forces into a supporting member must be located on the web.

2. Shear reaction (V) occurs where bending moment is changing (see handbook beam diagrams).

3. Shear stresses (S) combine to form diagonal compression which tends to cause the web to buckle.

Any weld joining the flange of the beam to its web is stressed in horizontal shear. In most cases, this weld is far enough away from the neutral axis so that the resulting horizontal shear is very low. These welds are not usually stressed very highly.

Concentrated loads, such as occur at bearing supports or at intermediate points on a plate girder, cause vertical shear forces in the web. These shear stresses resulting from vertical shear can be resolved into diagonal tension and diagonal compression stresses (Fig. 6). If diagonal compression stresses are high enough, they will have a tendency to cause the web to buckle. Intermediate stiffeners are attached to the web to prevent this (Fig. 6), or bearing stiffeners (Fig. 7) are used at bearing points. Stiffeners transmit these forces from the flanges where they are applied into the web.

The usual practice in riveted construction with intermediate stiffeners is to have them on both sides of the web, since no more holes in the web or rivets are required for the extra stiffeners. All that is actually necessary is to stiffen the web against buckling from the diagonal compression stresses. This can be done very easily by welding stiffeners on one side of the web only. Quite often this is done on the outside girders of a bridge, using two or more girders. Figure 1 (lead photograph) shows an all-welded girder bridge with no vertical stiffeners on the outside of the web. Bearing stiffeners are on both sides of the web. The elimination of stiffeners on one side obviously saves material, but a still greater saving is made in eliminating welding time.

Additional reductions can be made through a precise analysis of stress and loading conditions to determine if intermittent welds can be used in place of continuous welds. Figure 8 is a study of three different loaded beams. Beam 1 is simply supported with concentrated loads. Beam 2 has fixed ends and concentrated

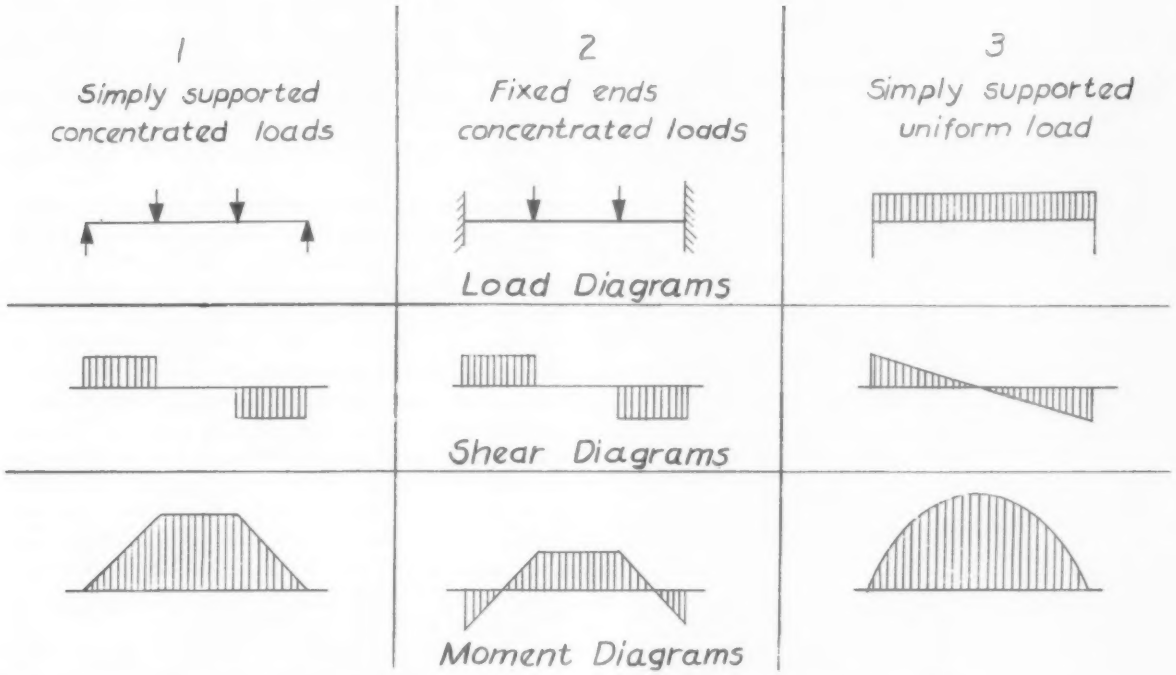


Fig. 8 Stress and loading conditions influence the amount of welding, as well as the location of welding

loads. Beam 3 is simply supported with uniform loading. The shear diagram and moment diagram for each beam is also shown. Notice the following:

1. Shear only occurs when the bending moment is changing.

2. It is possible for portions of the beam to have little or no shear. Note shear diagrams for beams 1 and 2. Here the bending moment is constant.

3. Fixed ends will shift the moment diagram so that the maximum moment is less. What is taken off at the center of the beam is added to the ends.

4. If there is any difference in shear along the length of the beam, it is usually greatest at the ends.

To apply these rules, consider the welded rigid frame in Fig. 9. The moment diagram for this loaded frame is shown on the left. The bending moment is gradually changing throughout the vertical portion of the frame. The shear diagram shows that this results in a small amount of shear in the frame, and using the horizontal shear formula would require a small amount of welding between the flange and the web; probably, intermittent welding would be sufficient. However, at the point where the crane moment is applied, the moment diagram shows a very fast rate of change. Since the shear value is equal to the rate of change in the bending moment, it is very high and more welding would be required at this section. As a general rule, continuous welding is used where loads or moments are applied to a member, even though intermittent welding may be used throughout the rest of the fabricated frame.

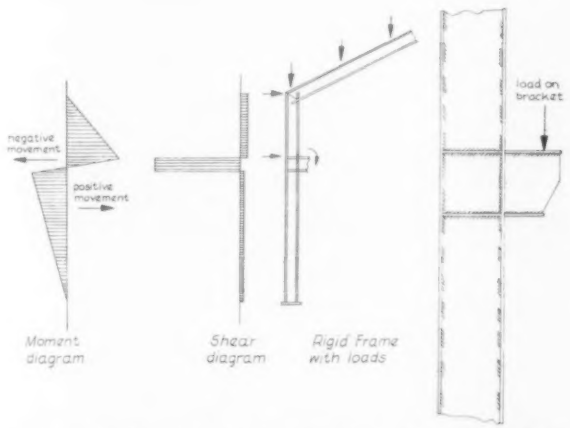


Fig. 9 Continuous welding is used at the point where moments or loads are applied to members. Intermittent welding may be used throughout the rest of the fabricated frame

Column Splices

Figure 10 shows several methods of making column splices without punching the main columns. In the first example (*a*), a plate and two angles are punched or, if necessary, drilled. The plate is shop welded to the top of the lower column. Two angles are shop welded to the upper column. After erection bolts are inserted, the upper column is field welded to the connecting plate. In cases where additional clearance is needed for subsequent erection of beams framing into the web of the lower column, it might be necessary to shop weld the plate to the upper column and then field weld in the overhead position in the lower column. If there is a difference in size of the columns, the connecting plate is designed as a member in bending, due to the misalignment of the flanges and its thickness determined from this. If the lower column is much deeper than the

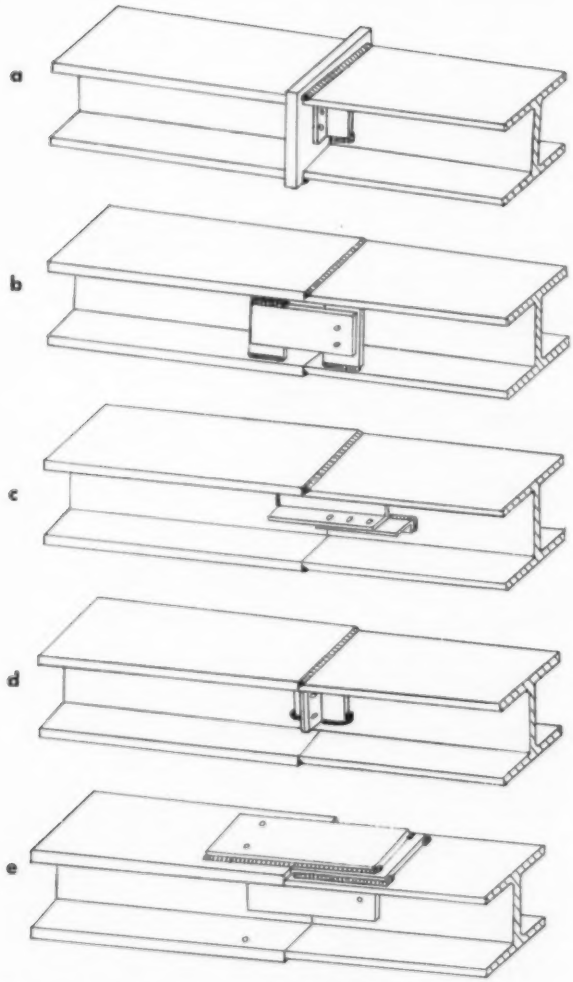


Fig. 10 Methods of making column splices

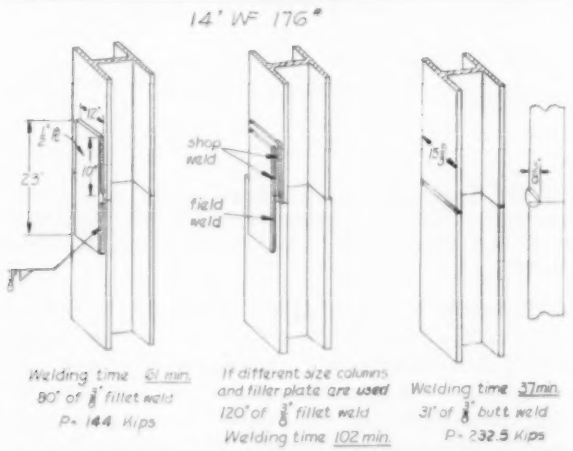


Fig. 11 Comparison of welding time for three different column splices

upper column, stiffeners can be welded directly below the flanges of the upper column.

In the second example (*b*), plates are shop welded to the inside faces of the flanges at the ends of both the upper and lower columns. A connecting plate is shop welded to one of these plates. The set of flange plates of the upper column and one end of the connecting plates are punched before welding. Erection bolts are used to hold these columns in position.

In the third example (*c*) and (*d*), four angles are punched and shop welded to the ends of the columns. Erection bolts are later added to hold the columns in position.

Sometimes the column splices are placed about mid-story in height in order to get the connection away from the region of heavy bending moment caused by wind-loads. The result is a connection sufficient to hold the columns in place and designed for shear only.

Where spliced plates are used and filler plates are needed, because of the difference in size of the columns (*e*), it is recommended that these spliced plates be shop welded to the bottom of the upper column. This allows the portion of the connection requiring the greater amount of welding, because of the added filler plates, to be welded in the shop where larger electrodes and higher welding currents may be used in the flat position, resulting in higher welding speeds and lower cost. After the upper column is erected, the spliced plate is field welded to the lower column.

Figure 11 is a comparison of welding time between a column connection using spliced plates and one in which the columns are butt welded. In this example, two 14-in. WF-176-lb columns are connected. In the case of the splice connection (*a*), 80 in. of $^3/_8$-in. field welds are required, half welded in the shop and half in the field. Using a 50% operating factor, this would represent about 61 min for welding. If the columns were of different size so that filler plates were required (*b*), this would double the amount of welding on the end of one column and would increase the welding time to about 102 min. On the other hand, if the ends of the flanges of the upper column were beveled slightly (*c*), in this case, $^3/_8$ in., and butt welded in the field, this would take about 37 min. Since butt welds are much stronger than corresponding fillet welds (a butt weld 1 in. thick and 1 in. long is figured for an allowable load of 20,000 lb, whereas a 1-in. fillet weld 1 in. long is figured for only 9600 lb), it does not take as much butt welding to be equivalent to a given amount of fillet welding. In this example, the small amount of butt welding used is still much stronger than the fillet welded joint.

Beam Connections

The most direct method of connecting a simple beam to a column is to land the beam on some sort of a seat attached to the column and then fasten the beam to this seat.

Several methods of temporarily fastening these connections have been used. Tack welding is unsatisfactory because it does not make allowance for plumbing the building before final welding.

Clamping the beams to the column seat is not always safe, although this has been used for "site erection" of lighter structures (see Fig. 12). The steel for this structure had been ordered cut to length and delivered to the site of erection. Temporary seat angles were clamped

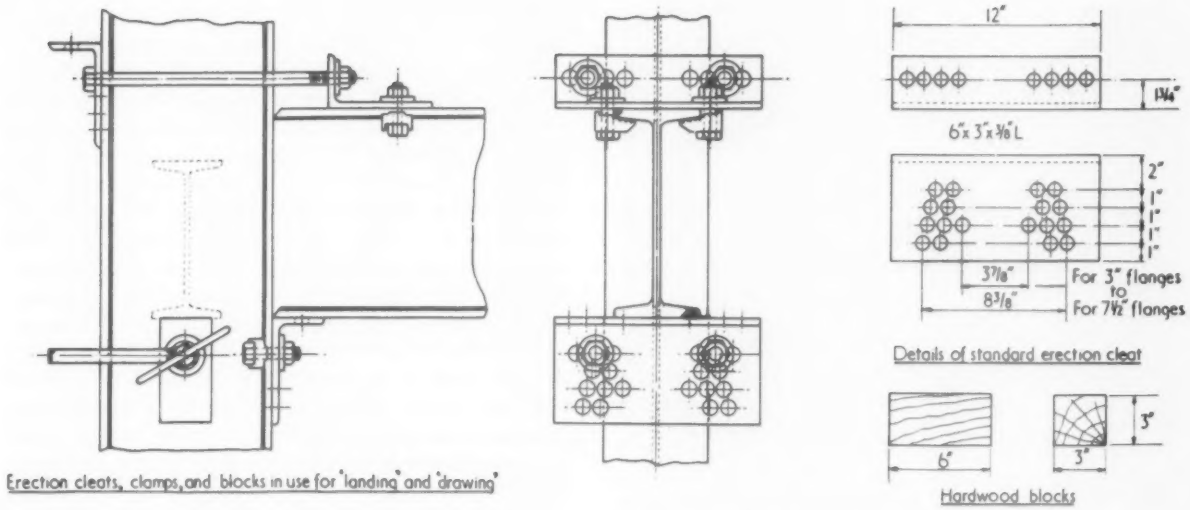


Fig. 12 Clamps can be used to hold beam and column connection for welding light structures

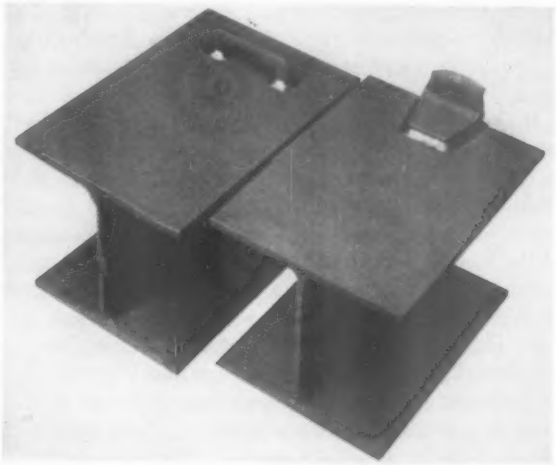


Fig. 13 The patented Saxe erection clips consist of a seat welded to the column (*left*) and a clip welded to the end of the beam (*right*)

Fig. 14 For erection, the beam (*top*) is put in position so that the clip drops into the seat (*bottom*). (Note excellent appearance of welds made with iron-powder electrodes)

onto the column at the proper position, and a temporary lug was clamped onto the top flange of the beam. The beam was hoisted into position and set onto the temporary seat angle of the column. A tie bolt was then screwed on, which pulled the beam into alignment with the column. The beam was welded directly to the column, and any temporary lugs were then disconnected and used over again.*

Special erection clips, which are welded to the beam ends in the column, have been used with success (see Figs. 13 and 14). These units consist of a patented forged steel clip and seat. The clip is welded to the end of the beam, and the seat is welded at the proper position on the column. During erection, the beam is put in position so that the clip drops into the seat. An adjustable clip has been developed to take care of possible poor fit-up between the beam and the column.

The use of a few erection bolts has been found to be a satisfactory way of temporarily fastening before final welding. Bolting may be done directly to main members or through small secondary attachments to the main members. It is less costly if small attachments are punched for erection bolting instead of moving the

* "The Case for Site Welding of Structures." *Brit. Welding Jnl.* (April 1956).

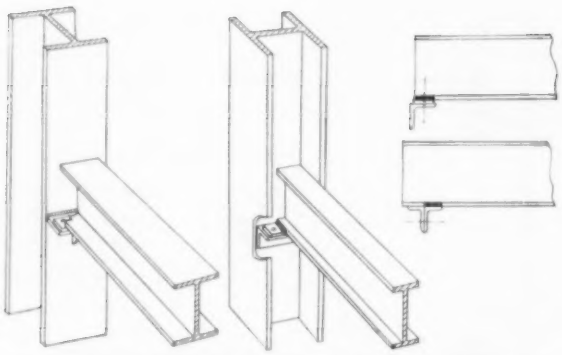


Fig. 15 Erection bolts are used to hold these connections for welding. No punching has been necessary on heavy members

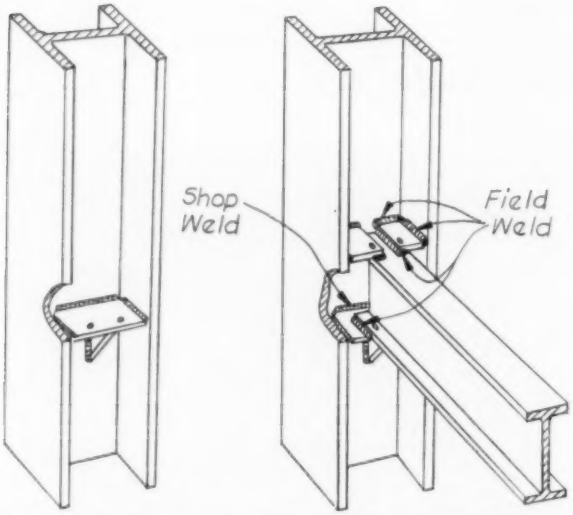


Fig. 16 This beam-to-column connection uses two flange connection plates instead of one to provide more space for fillet welds on the flange

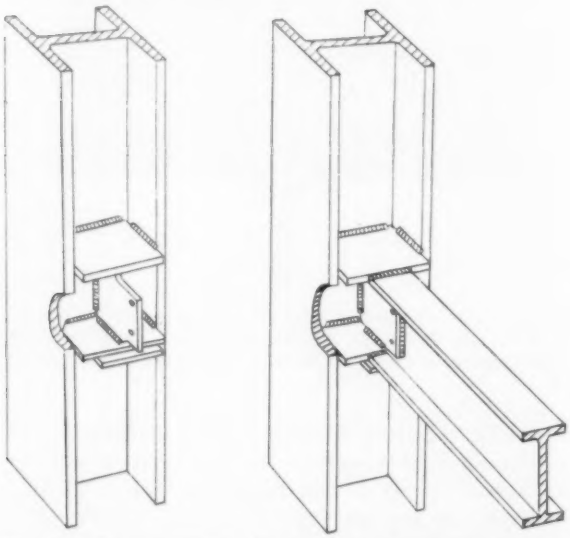


Fig. 17 Flange and web plates for this connection are shop welded. Flanges of beam are beveled for downhand field welding

heavy main members into the punch shop for punching. Many times a few holes are flame cut in the ends of the beams for erection bolts.

In Fig. 15 a small connection plate is shop welded to the bottom flange at the ends of the beams shown on the left. A seat is also shop welded to the flange of the column at the proper location. During erection, the beam is set on the seat and two erection bolts are used to hold them in place. The beam on the right is connected directly to the column web. A seat angle is shop welded to the inside face of the column flanges. Either a flat plate or angle is shop welded to the lower flange at the end of the beam. During erection, these are held in place by two erection bolts. All punching has been done on small plates or angles. No punching has been necessary on the heavy main members. Any of several methods may be used to tie in the top flange of the beam.

In Fig. 16, a beam frames into the web of a column. A seat, made of plate with a stiffener, is shop welded to the column. This stiffener carries the beam reaction. A pair of flange connecting plates are field welded to the column and the beam. By using two plates instead of one, more space is available for fillet welds on the flange to transmit the required load.

In Fig. 17, flange and web plates are shop welded into the column. Usually these plates are of the same thickness as the corresponding part of the beam which they connect. An additional plate fastened to the lower flange plates serves as a seat plate. The flanges of the beam are beveled for downhand butt welding in the field. The web plate laps the web of the beam and is connected by a fillet weld.

In Fig. 18, the beam frames to the column flange. The erection seat with the stiffener and the web connecting plate are shop welded to the flange of the column. A flange connecting plate is butt welded in the field to the column flange and fillet welded to the flange of the beam. Sometimes a backing strip is placed in between the connecting plate and the beam flange to insure complete penetration without any burn-through.

It is sometimes possible to design the seat stiffener

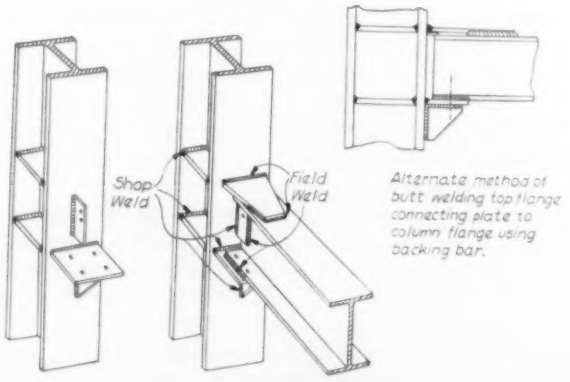


Fig. 18 Connections for welding beam-to-column flange. (Almost all welding is done downhand)

to carry all of the end reaction, thus eliminating any vertical welding in the field. This reduces the field welding to just downhand butt welding of the beam flanges to the column.

Where good fit-up can be assured, the beam flanges are beveled from the top side and butt welded in the field directly to the column flange. The beam web is cut back about 1 in. and fillet welded to the web connecting plate.

Where cover plates are added at the ends of the beams to carry the extra negative moment, these must be welded to the column for continuity (see Fig. 19). Flange stiffener plates of about the same thickness as the combined thickness of the beam flange and its cover plate are welded to the column. The upper detail of this figure shows the top beam flange and lower plate beveled to 45 deg. The lower flange and the top cover plate are left unbeveled. These are welded together in the shop. This arrangement produces a type of "J" groove for the weld, connecting them to the column flange. Instead of the usual thick flange plate stiffener at the column, this was replaced with two plates, each having half the thickness. This meant working with lighter connecting material and using two butt welds of half the size of the original single butt weld, which reduced the amount of welding on the stiffeners by half.

There have been some fabricating shops which have holes or pits in their floors so that the columns can be dropped down and placed in a vertical position, thus allowing much of the welding on the connecting plates to be made in the downhand position.

A recently erected seven-story, all-welded building was designed for as much downhand welding as possible, so that the new iron-powder electrodes could be used, both in the shop and the field. An E6024 electrode was used for the downhand fillet welds and an E6027 electrode was used for the downhand butt welds. It is claimed that an over-all saving in welding costs of

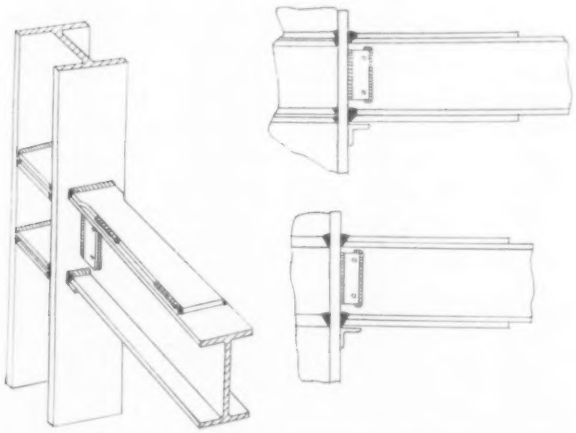


Fig. 19 Cover plates, when added for continuity, must be welded to the column. Stiffener plates are also welded to the column. Using two smaller plates for stiffeners, instead of one large one, reduces the amount of welding by half

Fig. 20 Joints in this building were designed as much as possible for down-hand welding so that iron-powder electrodes could be used. Savings of 50% in welding time were realized in making this column splice

Fig. 21 This beam-to-girder connection is made without using any erection bolts or clips. It develops full continuity

25% was obtained as a result of the use of the iron-powder electrodes.

Figure 20 shows the splice of two 14-in. wide flange, 158-lb columns in this building. A connecting plate was shop welded to the top of the lower column. The web of the upper column is field welded to this plate, using $^5/_8$-in. fillet welds on both sides. The flanges of the upper column are welded on the inside to this plate with $^1/_2$-in. fillet welds. It was estimated, using an operating factor of 50%, that this field welding would have required 89 min if conventional $^3/_{16}$ in. E6010 electrodes had been used. With a $^7/_{32}$-in. E6024 electrode, only 44 min were used. This saving of about 50% with the use of the iron-powder electrodes is a result of higher deposition efficiency, higher welding currents and less spatter.

Figure 21 shows a continuous connection used in a building in California for beams framing into girders. The web of the beam is cut back or slotted just below its upper flange so that the top flange of the girder will fit into it. A seat angle is shop welded to the web of

the girder. The beam is slid into place with its top flange resting on the girder flange and its bottom flange on the seat angle. The top beam flanges are butt welded together and to the girder flange. A long cover plate is added for the extra negative moment at this point. The bottom flange of the beam is butt welded to the girder web, using the seat angle as a backing strap.

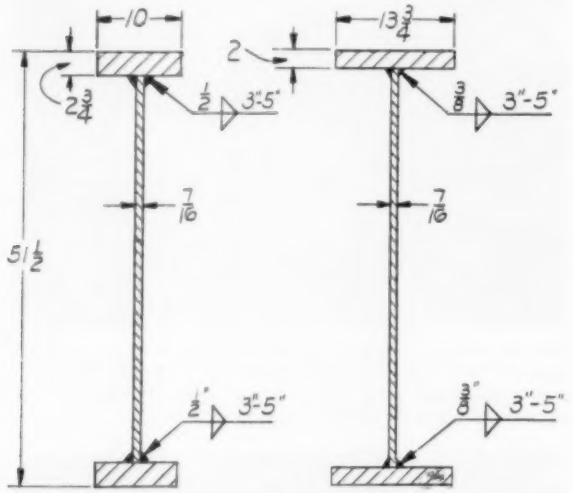
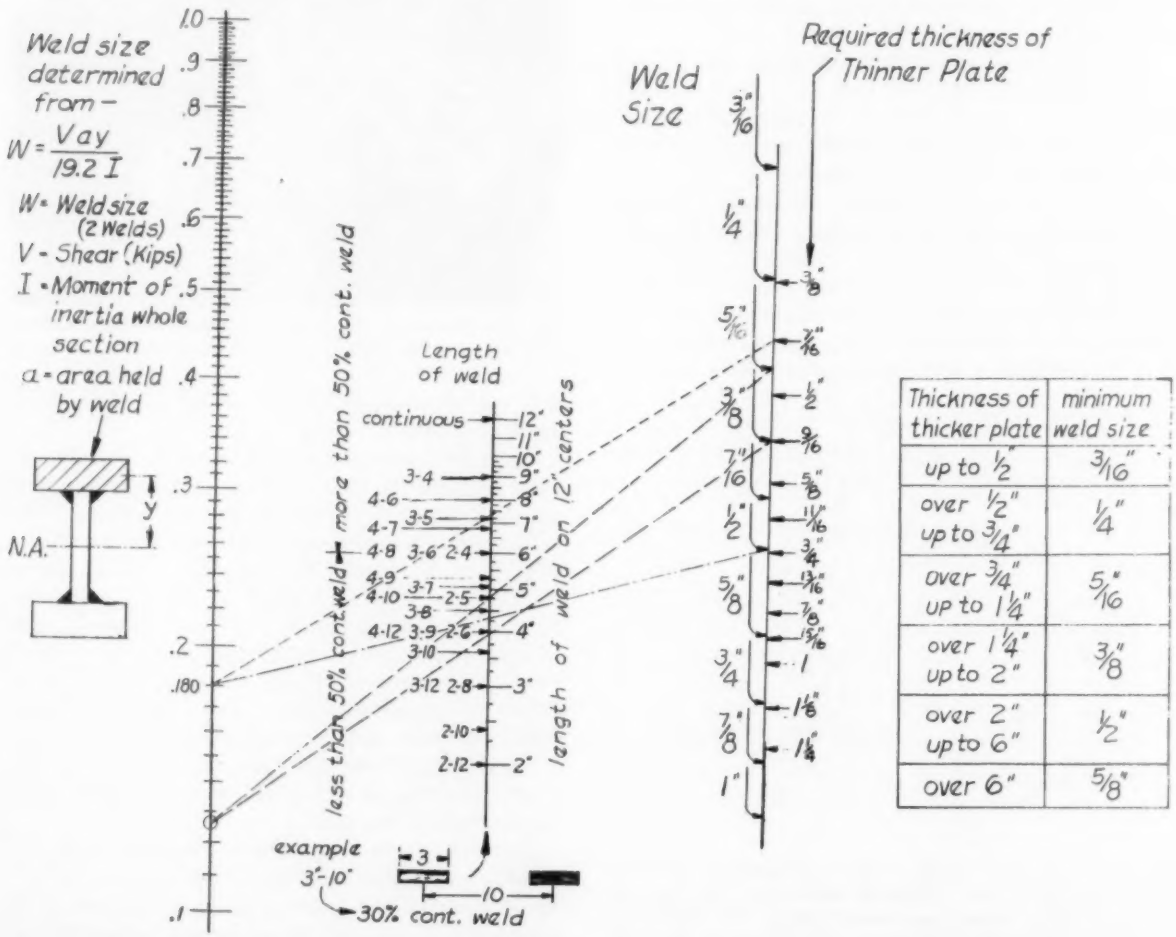


Fig. 22 Reducing plate size in flanges reduces required fillet size and saves 44% in welding time and cost

Weld Size

In Fig. 22, a plate girder has been designed, according to AISC, using $2^{3}/_{4}$-in. flange plates. Using the standard formula for horizontal shear, the required fillet weld size, if continuous, would be 0.180 in. The minimum size fillet welds, which can be used for designing purposes without stressing the $^{7}/_{16}$-in. web plate above 13,000 psi in shear, is $^{2}/_{3}$ of $^{7}/_{16}$ in., or 0.292 in. Dividing the required continuous fillet weld size (0.180 in.) by the maximum which may be used because of the web plate (0.292 in.), results in the percentage of continuous weld to be used, or 61%. A 3-to 5-in. intermittent weld produces a 60% weld and would be satisfactory. However, the AISC and the AWS have set up a minimum fillet weld size of $^{1}/_{2}$ in. for plates over 2 in. up to 6 in. in thickness; therefore, $^{1}/_{2}$ in., 3- to 5-in. fillet welds must be used.

If the designer is allowed to change the shape of the flange plate slightly so that a 2-in. plate could be used,



Thickness of thicker plate	minimum weld size
up to 1/2"	3/16"
over 1/2" up to 3/4"	1/4"
over 3/4" up to 1 1/4"	5/16"
over 1 1/4" up to 2"	3/8"
over 2" up to 6"	1/2"
over 6"	5/8"

Fig. 23 Nomograph for determining weld size and spacing

then the minimum size fillet weld would be reduced to $^3/_8$ in. The same spacing must still be used because of the $^7/_{16}$-in. web plate. This reduction in weld size results in a 44% saving in welding time and cost.

The nomograph in Fig. 23 will aid in determining weld size and spacing very quickly. The left-hand column is the fillet weld size required if made continuous. This value would be determined from the horizontal shear formula shown on the extreme left-hand side. The right-hand column would be the intermittent fillet weld size actually used, or else the thickness of the plate, if the plate is not to be stressed above 13,000 psi in shear. The middle column is the resulting length and center-to-center spacing of the weld. Also shown on this column is the length of weld for 12-in. centers.

In the problem just worked, a line would be drawn from 0.180 in. on the left column to plate size of $^7/_{16}$ in. on the right column. The answer read on the middle column is just above 3 to 5 in.

This nomograph has other uses. For example, assume an overhead joint requiring $^3/_8$ in., 4- to 12-in. fillet welds; that is, a 4-in. long weld with 12-in. centers. An overhead $^3/_8$-in. fillet weld must be made in about three passes. It will require more inspection, since there is some chance of it being made undersize. The fabricator would like to change this into a $^5/_{16}$-in. weld because he can make it in one pass with little chance of it being undersize. Using the nomograph, Fig. 23, a line is drawn from weld size of $^3/_8$ in. on the right column through 4 to 12 in. on the middle column to the left column. This point on the left column becomes a pivot point. From this pivot point, a line is drawn to weld size of $^5/_{16}$ in. on the right column, and the answer, 4 to 10 in., is read on the middle column at the intersection with the line. In other words, $^3/_8$-in. intermittent fillet welds, 4 in. long on 12-in. centers, may be replaced with $^5/_{16}$-in. welds, 4 in. long but on 10 in. centers. This results in a single pass weld instead of three passes and a saving of $12^1/_2$% in welding time and cost.

Table 1 has been prepared to aid the designer in determining weld length and center-to-center spacing. As mentioned before, the size of a continuous weld determined from the horizontal shear formula is divided by the size of the intermittent weld actually used. This will give the percentage of continuous weld required. Knowing this percentage, the designer can pick off the required weld length and spacing from the table.

Table 1—Weld Length and Center to Center Spacing

Continuous weld, %	*Intermittent fillet welds*		
75	..	3–4	..
66	..	..	4–6
60	..	3–5	..
57	..	..	4–7
50	2–4	3–6	4–8
44	..	..	4–9
43	..	3–7	..
40	2–5	..	4–10
37	..	3–8	..
33	2–6	3–9	4–12
30	..	3–10	..
25	2–8	3–12	..
20	2–10	..	..
16	2–12	..	..

Summary

If structural welding is to realize to the fullest its opportunities in the present time of booming construction, the cost of welding must be reduced to the point where it is the obvious selection from among competing methods. Approximately 85 to 90% of the cost of every welded joint is labor and overhead. To reduce this cost, joints must be designed to minimize the amount of welding and to permit maximum welding speeds. Joints must be designed for welding and not copied from riveted designs. They must be designed for efficient erection procedure. They should be designed for downhand welding wherever possible. Intermittent welding should be used where practical. Where practical, flange connections should be butt welded. For appearance, stiffeners can be eliminated on one side of plate girders. Frequently, plate thickness can be reduced to permit smaller weld size.

Weld length and spacing should be planned accurately. Iron-powder electrodes can increase welding speeds up to 50%, if joints are designed to permit their use. These are only some of the possibilities for economy in welding when structures are designed for welding.

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STUDY OF INTERRUPTED WELDING OF HEAVY-WALL STEAM PIPE

Investigation reveals that 2¼% chromium–1% molybdenum alloy-steel pipe in wall thicknesses up to ¾ in. can safely be welded with interrupted heat cycles if proper precautions are exercised

BY I. A. ROHRIG, J. O. SMITH AND E. G. SHIFRIN

An important question in the welding of large heavy-wall parts in steam power plant piping is whether an interruption can safely be permitted in the heat-treatment cycle by cooling the part to ambient temperature before postheating or at any time before the completion of all of the welding. If no interruptions are permitted in the heat cycle, the welding of heavy-wall alloy-steel piping may require a continuous operation of as long as a week, which would involve considerable expense in overtime work.

It was for these reasons that the Metallurgy and Piping Subcommittee of the Edison Electric Institute requested assistance from its members and from the Welding Research Council in determining from actual experiment and job experience whether interruptions can safely be permitted in the welding cycle of the low and medium-chrome alloys used in steam-power-plant piping and valves. The alloys referred to are exemplified in piping by ASTM Specifications A335, P11 (1¼% chromium, ½% molybdenum), P22 (2¼% chromium, 1% molybdenum), and P5 (5% chromium, ½% molybdenum).

Recently reported tests* have indicated that P11 and P22 materials have considerable ductility in the as-welded condition. There was still the question, however, of whether the material was sufficiently ductile to mitigate the stresses imposed on heavy-wall pipe welds made under conditions of restraint and cooled to ambient temperature before stress relieving.

At The Detroit Edison Co., field weld joints in chromium-molybdenum pipe have always been made with procedures that permitted an interruption in the welding and preheating after one-half of the weld thickness had been deposited, provided the cooling was done slowly and the preheating temperature was restored before welding was resumed. Until 2 years ago, however, our main steam pipe wall thicknesses had never exceeded 2½ in.

When the River Rouge Power Plant was designed, the piping selected for main steam service was ASTM A335, P22, with a wall thickness of 3¾ in. It was desired to be able to conduct the welding of this piping in interrupted cycles in order to avoid excessive overtime costs. In addition, it was desired to stop welding after the weld-metal thickness reached 1 in. and take a radiograph. This was proposed in order to obtain a radiograph with maximum detail for the important first inch of metal where most defects occur, and to have minimum weld metal to remove in order to repair any defect located by means of a radiograph.

Acting upon the results of the experiment described in this paper, our company has made it a standard procedure to permit interrupted welding cycles on P22 material at any time after the deposition of the first inch of weld-metal thickness, provided the joint is cooled slowly and uniformly and provided the preheating temperature is restored before welding is resumed. About thirty 3¾-in. wall pipe joints of 2¼ Cr–1.0 Mo alloy steel have been welded in this manner without any apparent difficulty as the result of interruptions in the welding. Fifteen of these welded joints are now in service at 1050°F and 2100 psi.

Test Procedure

In designing the test fixture, it was desirable to simulate conditions of restraint of a nature as severe as any that might occur in the field. It was impractical, however, to impose sizable bending moments on the short lengths of pipe containing the test weld. In addition to localized shrinkage stresses, relatively high tensile stress due to bending may be superimposed on a weld

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*"The Arc Welding of 2.25% Chromium—1.0% Molybdenum Alloy Steel Pipe," *Welding Research Supplement*, Vol. XXI, No. 4, 181-s to 194-s, (April 1954).

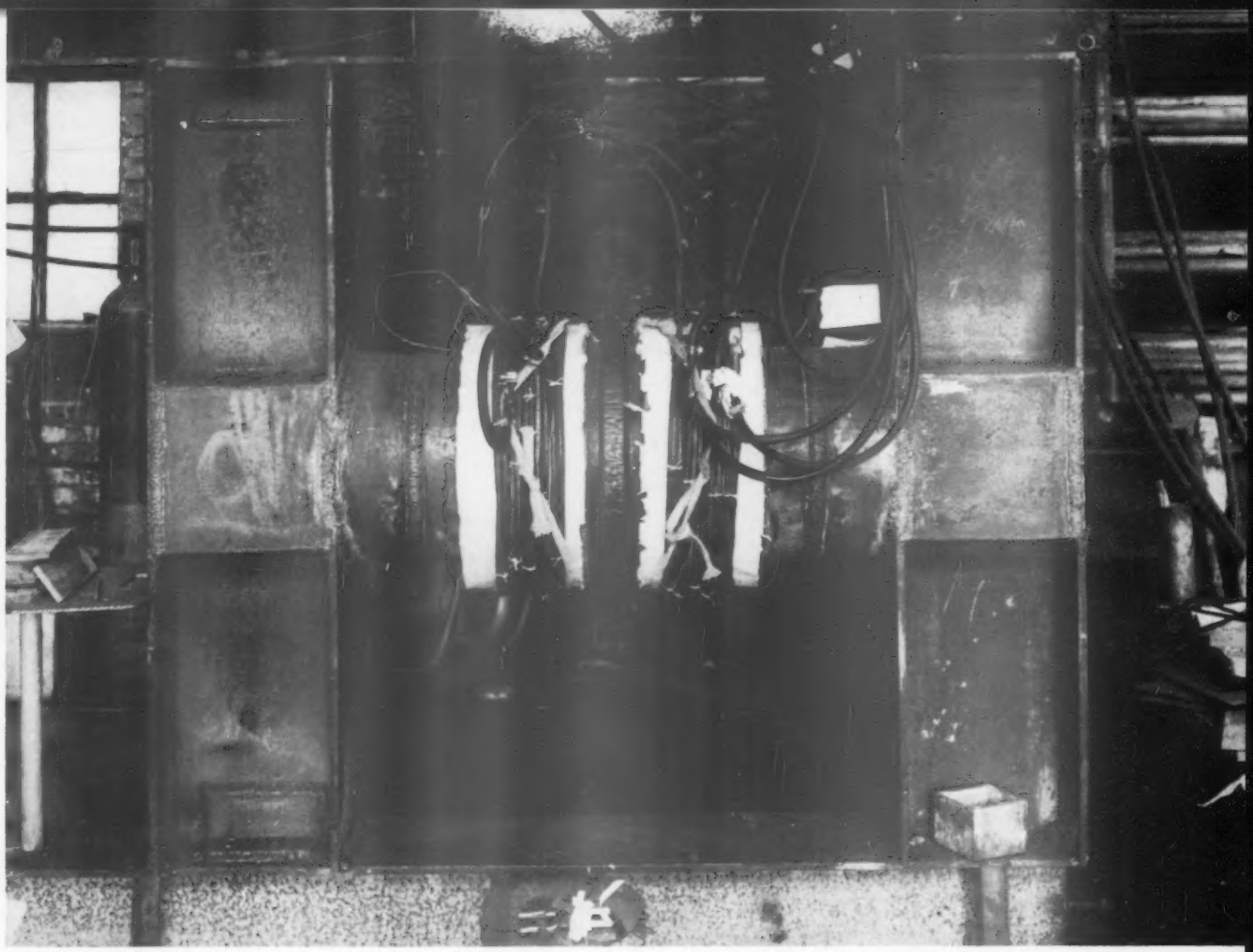

Fig. 1 Test frame used for second interrupted welding test

cross section as the result of the release of axial and lateral restraints on the section of piping containing the weld. If normal precautions are observed, bending moments due to lateral restraints are not present at the location of the weld. However, in cases where lateral restraints employed to force the joint ends into alignment are accidentally removed soon after welding is started, relatively high bending stresses can be imposed on the existing weld cross section. In an interrupted welding procedure, the chances of such accidental removal of restraints are somewhat greater than in uninterrupted procedures because the weld location may be left unattended during the period of interruption. Therefore, the possibility of such stresses being imposed upon the unfinished joint must be considered. For this reason, it was decided to mount the test pipe in a heavy frame that would afford axial restraint to thermal shrinkage many times more severe than could exist in the field, thereby introducing conditions that would produce stresses of the same order of magnitude as those that would be introduced by normal cooling shrinkage plus accidental bending.

The total axial force acting on the pipe at any time during the tests was determined by measuring the sum of the axial forces in the horizontal members of the frame. The combined forces in these members are at equilibrium with the axial pipe force—therefore are equal and opposite in sign to the latter at all times. Axial forces in the frame members were computed from strain measurements made at locations along the centroidal axes of these members. For this purpose, SR-4 bakelite strain gages, Type EBD-1S, were employed. This is a special purpose gage designed to minimize the errors in strain measurements normally encountered due to changes in temperature of the surface to which they are bonded. Figure 1 shows the test frame with the strain gages bonded to the horizontal members.

The final assembly of the vertical members to the horizontal members of the frame was carried out with the still-separated sections of the test pipe heated to 650°F and spaced $^3/_{16}$ in. apart. Thereby, the proper root opening of $^3/_{16}$ in. between the two halves of the joint was assured when the pipe was subsequently pre-

heated prior to welding. After assembly of the frame, the entire setup was cooled to room temperature and initial or "zero" readings were taken on the strain gages. The pipe ends were not tacked together until the pipe was reheated and welding was about to begin.

The pipe section used for the tests was of standard P-22 composition, was $3^{11}/_{32}$ in. in wall thickness, $17^{5}/_{16}$ in. OD, and 4 ft long. It had been made by the cup-and-draw process. The bevel used is shown by Fig. 2.

The electrodes used corresponded to electrode designation E10016 and deposited metal which matched the chemical analysis of the pipe metal. The electrodes were stored in a heated cabinet to prevent absorption of moisture into the coating and were kept warm in a portable electric heater until the moment of use.

The first test was carried out in ten separate interrupted cycles. For each cycle, the pipe was preheated to a temperature between 500 and 600° F by induction heating coils and maintained within that temperature range while welding was in progress. Figure 3 shows the joint with the induction heating coils in place. Following the planned amount of welding, the pipe was covered with asbestos blanket or pipe covering and permitted to cool to room temperature, at which time force measurements were made. After each cycle of preheating, welding and cooling, a gamma-ray inspection of the weld was performed to check for cracks or flaws that might have developed due to interrupting the welding process. The radioactive source (iridium 192) was positioned at the center of the pipe for this purpose.

After completion of this test, the weld was machined out and a second test conducted. The second test was similar to the first except that greater depths of weld metal were deposited during each cycle of welding. (The depth of the first layer was $1^{1}/_{32}$ in. as compared to $^{5}/_{16}$ in. for the first test.) The welding was completed in eight interrupted stages. Following the second cycle of welding, when the weld thickness was $1^{7}/_{16}$ in., the pipe and weld were chilled with dry ice to simulate conditions that might exist if welding were interrupted in exposed locations during extremely cold weather. The temperature of the weld was lowered to −3° F. The pipe temperature varied from 0° F near the weld to 30° F near the ends. Measurements of the increased tension in the weld due to shrinkage were made as in the case of normal cooling following the welding cycles.

Following completion of the second test, specimens for tensile and impact tests were machined from the weld. Half of the specimens were stress relieved prior to testing, with the remainder tested in the as-welded condition.

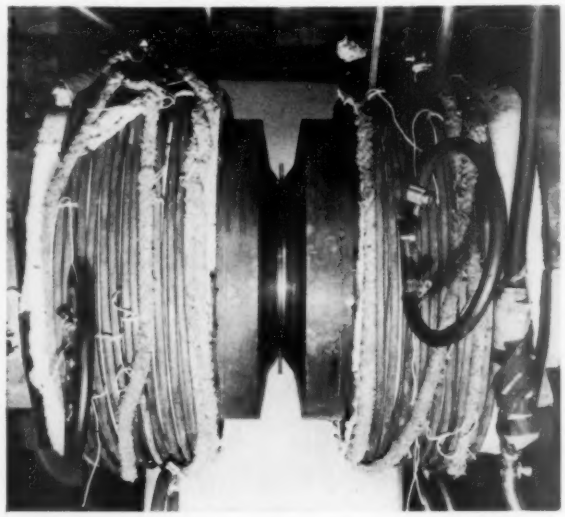

Fig. 3 Test joint prior to welding

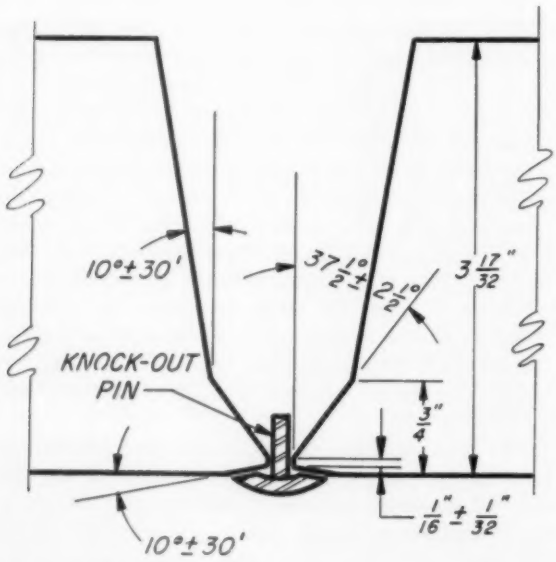

Fig. 2 Joint design

Results and Discussion

The total axial force imposed on the weld was in the order of 300,000 lb for the second and more severe test. This was somewhat higher than the corresponding value for the first test because of some stiffening of the frame that had been done subsequent to the first test.

The total axial force remained essentially the same as the welding progressed. This was to be expected, since the magnitude of the axial force is determined predominantly by the thermal shrinkage during cooling from the preheating to the ambient temperature and the restraint against that shrinkage. Since the axial stress on the weld is equal to the total force divided by the cross-sectional area of the weld, the stress decreased with increasing thickness of weld metal.

The results of the force and stress measurements made during the second test are shown in Fig. 4. As shown by this graph, the force on the cooled weld after the first welding cycle was 295,000 lb. The maximum tensile stress on the weld cross section was 7500 psi. The stress curve shows an exponential-type decrease with increasing depth of weld metal. An extrapolation of this curve to lesser weld thicknesses

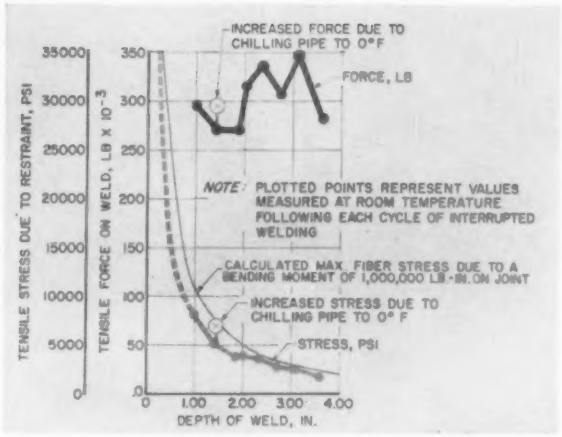

Fig. 4 Tensile force and stress on weld cross section due to external restraint—second interrupted welding test

shows that the stress after cooling would increase very rapidly with decreasing weld metal thicknesses below 1 in.

Based on flexibility calculations made for this heavy-wall pipe, the lateral forces that would normally be required to align a representative joint would, if released, create a bending moment in the order of 1,000,000 pound-inches at the location of the joint. The maximum axial fiber stresses that would be imposed on the test weld under this moment are plotted against weld depth in Fig. 4. It can be seen that the magnitudes of these stresses and their variation with weld depth are essentially the same as the stresses actually imposed on the test weld due to axial restraint.

The results of the radiographic examinations, as well as physical tests performed on samples cut from the completed welds, indicated that no cracks were present in the weld of either test. Table 1 shows the results of the physical tests, in which all tensile failures occurred outside the weld and the specimens had the ductility expected of sound welds.

Although no cracking was observed when the welding and preheating were interrupted at weld metal thicknesses of less than 1 in., it was decided to permit interruptions in field welds only after the deposition of at least 1 in. of weld metal. The stress due to external restraint "levels off" at approximately 1 in. of thickness and a weld of this thickness would not be unduly stressed by bending moments that could be accidentally introduced.

The additional axial tensile force imposed on the weld as the result of chilling the pipe was approximately 26,000 lb, as is shown in Fig. 4. This increase in force was relatively small, and it produced no adverse effects. Therefore, it was felt that the conclusions of this investigation would remain valid even though lower ambient temperatures might be encountered. Of course, lower ambient temperatures require greater care in insulating the surface during cooling.

In addition to the axial stress caused by external restraint, circumferential and radial residual stresses are present due to the contraction of the weld metal. There is no appreciable increase in these stresses as the result of cooling following interruptions in the heat-treatment cycle, provided normal precautions are taken to prevent excessively rapid and nonuniform cooling. If such precautions are neglected under severe conditions, such as outdoor winter weather, it is possible that circumferential and radial thermal stresses may be developed of a magnitude nearly as great as the axial stresses due to the restraint imposed on the joint in these tests.

Table 1

Tensile tests on weld specimens

	Yield stress, psi	*Ultimate strength, psi*	*Reduction in area, %*	*Elongation in 3 in., %*
0.505 round specimens				
As-welded	62,200	92,800	63.8	...
	48,500	82,200	70.4	...
Stress relieved, 1300° F	45,900	77,000	72.0	...
	48,500	77,000	70.4	...
Flat coupons, 2.5 × 0.370 in.				
As-welded	54,100	77,500	...	...
	52,400	75,800	...	19.8
Stress relieved, 1300° F	43,200	70,400	...	20.5
	51,100	71,500	...	5.5

Charpy impact tests on weld specimens, V-notch

	Temperature of test, ° F	*Impact, ft-lb*
As-welded	32	139
	32	15
	32	110
	70	36
	70	95
	70	30
Stress relieved, 1300° F	32	125
	32	157
	32	150
	70	137
	70	146
	70	155

Conclusions

P22 pipe in wall thicknesses up to $3^3/_4$ in. can safely be welded with interrupted heat cycles provided:

(*a*) One-inch of weld metal has been deposited.

(*b*) The preheated and welded area is suitably insulated to assure uniform and reasonably slow cooling to ambient temperatures.

(*c*) Preheating temperature is restored prior to the resumption of welding.

(*d*) Low-hydrogen type electrodes are used. (Welds made with other types of coatings were not studied in this investigation.)

Typical parts produced by metal gathering and subsequent forging operation

METAL GATHERING BY THE RESISTANCE-HEATING PROCESS

General description of the usefulness of the process, the variables that must be controlled, techniques involved, application information and results obtainable

BY W. F. HAESSLY

SYNOPSIS. "Metal gathering" or "progressive hot upsetting" by the 60-cycle resistance-heating method is having greater use in the forming of such metals as Silchrome valve steels, jet engine turbine blades, stainless steels, titanium, etc., as well as the more common metals.

Present-day usage of high-temperature alloys demands the use of metal gathering as a means to assist obtaining the final form where limited working temperatures must be respected. Exceeding these temperature limits in some cases will create a transformation so that subsequent heat treatment cannot restore the original properties. Forging at the elevated temperatures is more difficult and die wear is excessive. Gathering simplifies forging operations under these circumstances by reducing the extent of flow required in the forge press. In many instances, the number of forge operations can be decreased.

This paper presents a general description of the usefulness of the process, the variables that must be controlled, techniques involved, application information and results obtainable.

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Introduction

Metal gathering is a process that has been known for

many years. Its use in the past has been limited because many manufacturers complained of the lack of control over the degree of heating, resulting in variations of the gathered formation exceeding reasonable tolerance limits. Present-day equipment of superior design is largely responsible for the rapidly increasing usage of this process. It has now been accepted by leading manufacturers in this country for mass production usage and new applications are found every day.

The term of "resistance upsetting" generally refers to conditions where material is firmly gripped between dies and the extended end collapses under the application of pressure when sufficiently heated by the passage of high-amperage current. The length of unsupported stock extending from the dies is limited and this determines the maximum volume of the upset that can be acquired.

Resistance "metal gathering" extends the limits of resistance upsetting since material is forced through sliding contact dies. The volume that can be acquired using this method is almost unlimited.

Resistance upsetting and resistance metal gathering are terms frequently misinterpreted. The two processes are similar only to the extent that high-amperage current is used for heating purposes. Upsetting has definite limitations that are extended by gathering and this makes it desirable to further differentiate between the two processes.

Resistance Upsetting and Its Limitations

For explanation purposes let it be assumed that the work part is a round rod securely gripped between jaws of a movable clamp as illustrated by Fig. 1A. Pressure applied by the movable clamp forces the rod end against a stationary die block completing the secondary circuit prior to the passage of current for heating the extended end. Highest temperatures will be produced at approximately the mid-point between clamping jaws and the fixed die block. Figure 1B shows the upset formation that will be developed when the rod is heated to within the forging-temperature range.

The shape of the end contacting the anvil is of interest since it reveals that upsetting started and progressed from the mid-point. The end contacting the die block was not at sufficient temperature to flow and the application of higher pressures would only force the cold end to sink into the upset formation. This creates "end folds" or what is commonly referred to as "cold shuts."

A second limitation for the upsetting process is that the unsupported length extending from the clamp jaws is limited to approximately three times the diameter of the work part to be processed. Symmetry of the upset formation cannot be controlled and injurious buckling is expected if the extended end exceeds this amount. For all practical purposes, it is limited to $2^1/_2$ times the work diameter since it will then be less sensitive to squareness of the work end, uniformity of heating, etc.

The upset volume can be increased by reclamping the upset formation made according to the Fig. 1 method and repeating the same cycle. This principle is illustrated by Fig. 2 and it does extend the volume that can be acquired. However, the limitations are self-evident, being that the formation is not homogenous since it is built up in a laminated manner. Further-

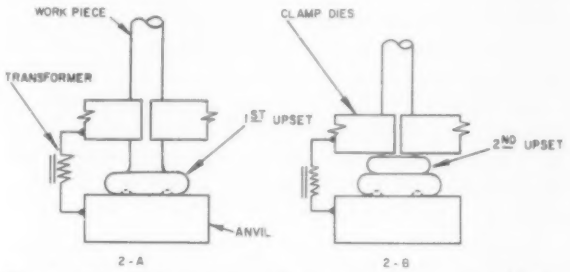

Fig. 2 Schematic representation of resistance upsetting by multiple upset method

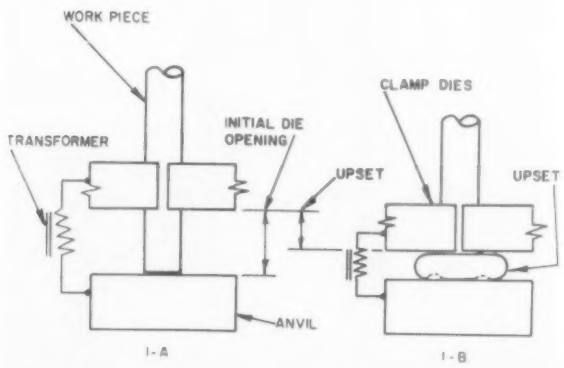

Fig. 1 Schematic representation of resistance upsetting process

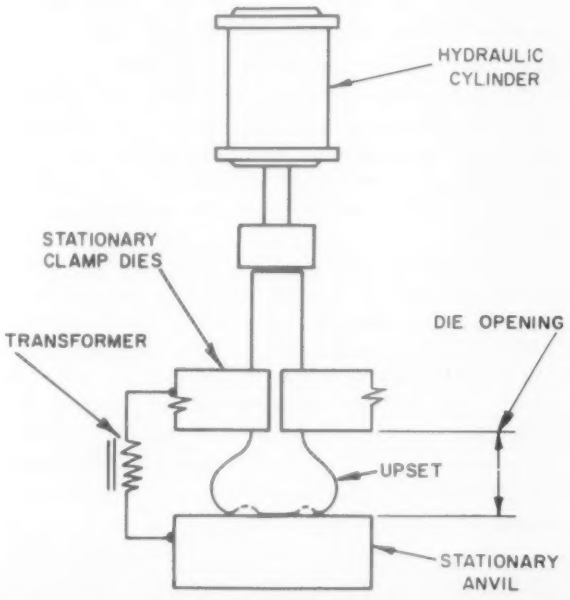

Fig. 3 Representation of limited metal gathering operation where clamping jaws are stationary

more, the formation is made in an inefficient manner since time is lost reclamping, etc.

Operating Principles of Resistance Metal Gathering

Another method for increasing the volume of upset is according to Fig. 3. Distance between stationary clamp dies and the anvil is limited to approximately $2^1/_2$ times the rod diameter, as explained for the Fig. 1 method. Material is presented to the gather zone at a controlled rate by slipping through the stationary clamp jaws. Shape of the gathered formation is limited since die opening must be preset at a fixed dimension and, furthermore, the fact that die opening is fixed and limited still restricts the volume that can be gathered.

The principle of Fig. 3 method can be further extended by varying the distance between the anvil and the clamp jaws at a controlled rate according to Fig. 4. Distance between clamp jaws and the anvil is a minimum at the start of the cycle to insure heating of the end and it is increased as the gathered formation grows in size. A hydraulic cylinder applies pressure to the end of the rod and forces it through the sliding clamp jaws as the end of the rod contacting the anvil is heated by high amperage current and progressively collapses.

Figure 5 shows the anvil and retractable jaws of a gathering machine with a round rod clamped in working position at the start of the cycle. Distance between clamp jaws and the anvil is a minimum at this time so that heating is confined to the end of the workpiece. The transformer is connected according to the Fig. 4 method.

Figure 6 is of the same machine showing the gathered formation and position of the clamp jaws at the end of the cycle. Metal between the clamp jaws and the anvil is at a uniform elevated temperature according to visual indications. The rod portion above the clamp jaws is heated only by conduction from the gather zone.

Resistance-Gathering Metal-Flow Rate

Figure 7 shows the average rate that rod material can be fed into the gathered formation. This plot does not include time required for initial heating of the rod end. This will depend on temperature and material of the anvil insert and end preparation (if used) on the workpiece.

Initial heating time is not of importance for rough estimating purposes since it is short in comparison to the time required for the complete cycle.

Figure 7 applies only for round or rectangular bars where width/thickness ratio is less than 5:1. Special sections may require longer times and must be analyzed accordingly. Some upset shapes are produced by modifying the operating cycle and this must also be taken into consideration.

Types of Gathering

Gathering may be segregated into two classifications; namely, end gathering and mid-point gathering. Figure 6 shows an end-type gathering which is made by forcing the end of the work part against an anvil and

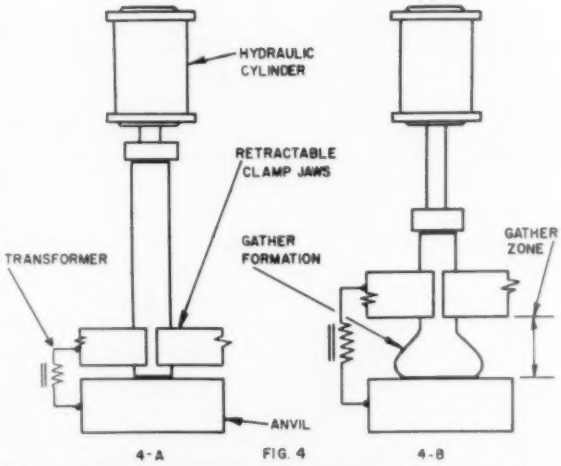

Fig. 4 Schematic representation of resistance metal gathering

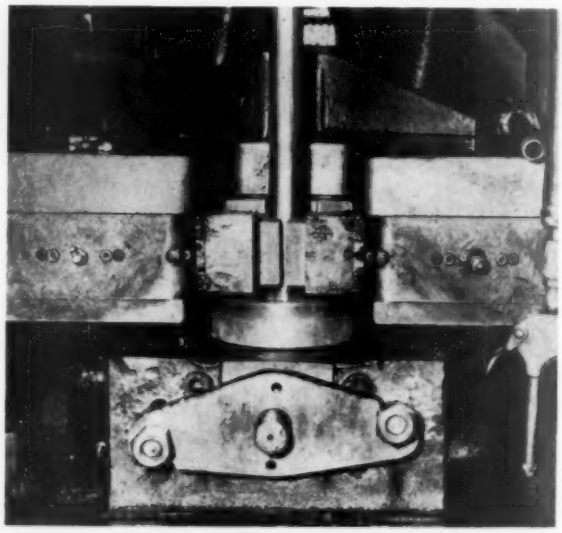

Fig. 5 Workpiece clamped in position at start of metal-gathering cycle

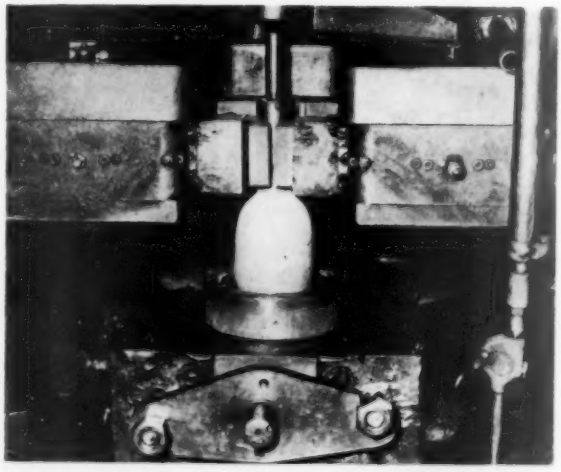

Fig. 6 Workpiece in machine at end of gathering cycle

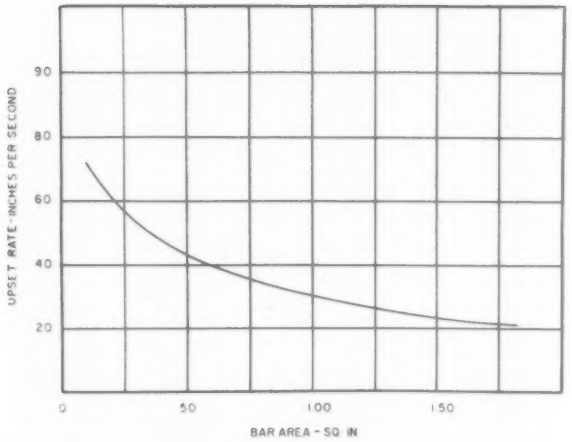

Fig. 7 Average gathering rate for most ferrous materials

Fig. 8 Various end formations created to determine best shape for final press forging to achieve product at left

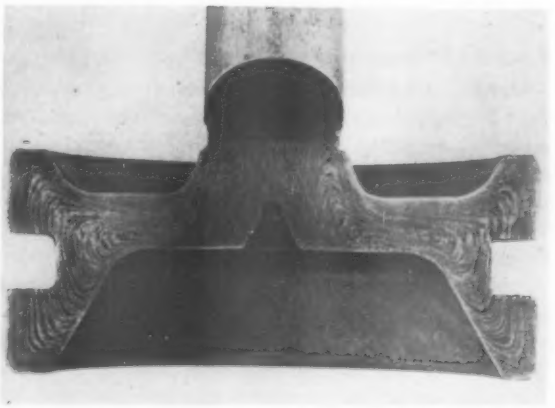

Fig. 9 Etched macrosection of a type of end gathering, showing flow-line pattern after press forging and machining operations

the gathered formation will be developed within this zone.

The anvil may be replaced by a second pair of clamp jaws for solidly gripping around the work part and in this manner the gathered formation can be made at any point along the workpiece. This type gathering would be referred to as mid-point gathering.

Either type is applicable for round or rectangular bar stock and the same techniques can be applied on tubing to a limited degree.

Operations for mid-point gathering are carried out in a manner similar to end-type gatherings.

How Gathered Sections Are Used

Gathered sections are frequently shaped to the desired form by forging means. In some cases, the gathered formation can be made so near the final shape that it can be finished with a single blow on a forging press. Simple forging operations are usually done immediately after upsetting without further heating being required.

Shape of the gathered formation is still of importance where forging means are later used to obtain the final form. The purpose is to keep mechanical abrasion or what is commonly referred to as "die wash" at a minimum. Figure 8 is an example showing the various shapes of an end-type gathering that were made to decide which was best for producing the forging shown at the left. Some flow during forging is necessary and the desirable location of this excess metal may vary depending on the material, shape of forging, etc.

Precise control of the amount of metal supplied to the gathered formation permits forging without flash and subsequent trimming may not be required.

Machining and grinding are also used to obtain the final form.

Macro Examination of Gathered Formations

Hot working or rolling bar stock to shape causes grains to be elongated in the direction of work. Macroetching of a section parallel to the direction of work reveals this as continuous lines commonly referred to as flow lines.

Figure 9 is a macrosection of a gathered formation. The flow lines are continuous, without sharp folds or bends, and follow the general shape of the gathered section. These lines indicate the direction metal has flowed as it was worked and supplied to the gathered formation.

Properties of steel, such as resistance to impact, corrosion, ductility, etc., may differ in a direction parallel to flow lines as compared with a perpendicular direction. Some manufacturers have reported that components made by resistance gathering will outlast those made using other methods in the order of 4 or 5 to 1. Advantageous grain flow is given as the reason.

Anvil Insert Material

The anvil insert is required to withstand high compressive and abrasive forces at elevated temperatures for repeated operating conditions. It must also be

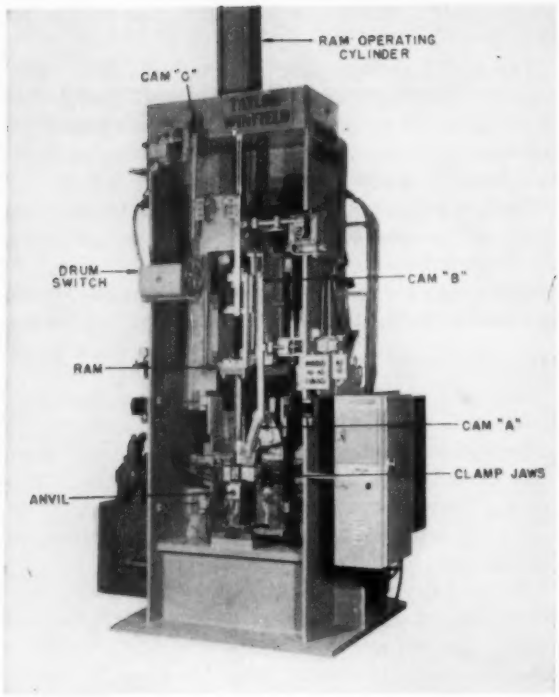

Fig. 10 Metal-gathering machine, general purpose, hydraulically operated

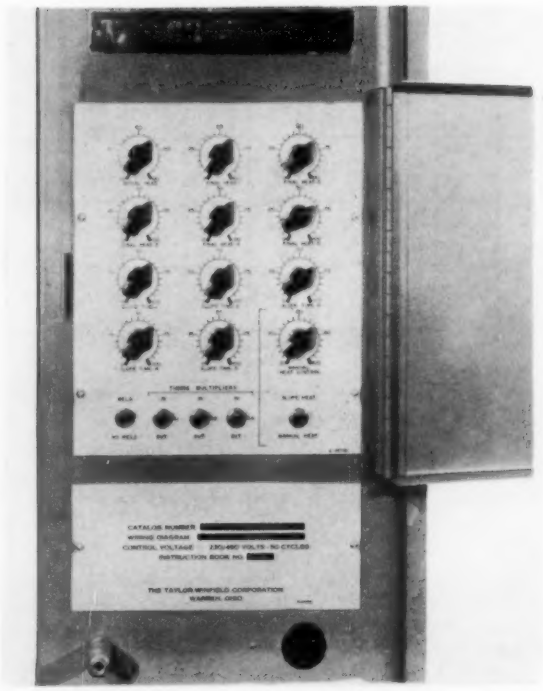

Fig. 11 Heating control for metal gathering operations—operator's adjustment panel

resistant to heavy scale formations detrimental to the passage of high-amperage current since it is a part of the secondary circuit.

High resistivity and low thermal conductivity are desirable properties of insert material for end type gathering since there will be less tendency for such an insert to extract heat from the gather zone. This factor is particularly important where upset diameter is large compared to rod diameter. Insert material of high thermal conductivity will rapidly conduct heat from the rod end which encourages upsetting to start and progress other than at the extreme end. The tendency for folds in the grain flow at the end will then persist and it will also affect symmetry of the gathered formation.

It is occasionally desirable to preheat the anvil insert to an optimum initial operating temperature with a torch or by processing a few workpieces before starting continuous production operations. Water flow through the insert support is then adjusted during successive operating cycles to that required under the particular set of operating conditions.

It is possible to add solenoid operated valves to the die cooling system; also to install a separate die heating transformer with a suitable contactor and thermocouple temperature sensing device.

"Heavy Metal" has been extensively used in the form of inserts shrunk-fit into a case or jacket made from Inconel "X." An interference fit in the order of 0.005 in. per diametric inch is generally used.

Inserts made from 55B Carboloy and 30W3 Elkonite approximately $^{5}/_{16}$ in. thick and brazed to RWMA Class No. 2 copper alloy are being used by one manufacturer for upsetting operations where anvil temperature is not of appreciable importance. Water cooling must be used to prevent destroying brazed joints where used in composite anvil construction. This limits the extent that the die insert can be heated by supplemental means.

Various types of processed cast iron and nodular iron have been used to a limited extent for experimental and tryout purposes. This material retains most of its properties at elevated temperatures and it is easily prepared in various shapes and forms. It is applicable where a limited number of parts are to be processed.

Material Selection and Preparation

Material presented to the machine for gathering must be clean, free of mill scale and sufficiently smooth to slide through the contact dies without objectionable wear on the dies. Some manufacturers use a ground finish and others use cold-rolled stock. Excessive die wear may be expected if hot-rolled, pickled and oiled material is used.

Material ends should be free from burrs that would localize starting current to the extent that arcing may occur. This would damage surface of the anvil insert and the heat from the arc may even be of sufficient intensity for the rod end to stick fast to the anvil.

A conical end preparation is frequently used on round bar stock for end type gatherings. The diameter making initial anvil contact is thus reduced to approxi-

mately one-third of the rod diameter. This type preparation encourages heating to start at the very end of the rod and in this manner helps to prevent end folds.

Pressure must be reduced at the start of the cycle when conical end preparation is used to avoid applying high unit pressures that would decrease die life.

Material must be free from surface of subsurface defects since they may cause external splitting of the gathered formation. Centerless grinding will remove these defects at the same time it prepares a surface suitable for gathering.

Draw-die marks must be a very minimum on material that is not provided with a ground finish. These marks will prevent suitable contact with the clamp jaws for passage of current and may be to the extent that it will affect symmetry of the gathered formation.

Materials that have been extensively gathered on a production basis are low- and medium-carbon steels, alloy steels including types 4130 and 4140, stainless steel, Silchrome valve steels, titanium alloys and aluminum alloys. The titanium alloys have been gathered as easily as stainless steel. In fact, stainless steel has been used as set-up material for the titanium during the course of past development programs.

Basic Requirements of Gathering Equipment

Metal gathering is accomplished on the product which is maintained in the hot-working temperature range. Top of the range is limited by a margin of safety below incipient melting or undesirable transformation in alloy materials. The lower end is limited by the temperature at which strength increases and plasticity decreases so that excessive pressures are required to cause flow, which may rupture the metal and damage the anvil.

The gathering zone is heated by its own resistance with its temperature controlled by the current that is passed through the work. The resulting degree of plasticity determines the amount of pressure that must be applied for feeding material into the gather zone. The required pressure is, therefore, dependent upon the current level and time used for heating the work area.

Once gathering has started and temperature remains uniform, the rod will be fed into the gather zone at a near uniform rate. Therefore, retraction of the clamp jaws can also be associated with ram movement since jaws must be retracted to change position of the hot zone and provide clearance for the gathered formation.

Thus, ram movement for feeding material into the gather zone, and retraction rate of the clamp jaws may be interlocked so that one is dependent on the other. Identical gathered formations can then be produced, irrespective of slight variations in end preparation, surface conditions of material, contacting surface of the anvil, etc.

Rate of heating of the gather zone is proportional to the square of the applied current. The hot zone may be heated momentarily to the danger point as current is suddenly increased if no provision is available to compensate for the change. If heating is too rapid, it will cause uneven expansion of the metal and be the cause of internal cracks. This is especially dangerous for carbon steel, high-alloy steels and other sensitive metals.

Overheating is also apt to occur if the rod is fed into the gather zone too slowly. This overheating occurs between the upset formation and the clamp jaws.

Excessive product temperature changes can be avoided by supplying an unlimited volume of oil at a predetermined pressure level to the hydraulically operated piston so that the ram is required to flow metal into the plastic gather zone as required.

"Constant pressure" is the term used to describe this principle of a maintained upset force. Voltage adjustments can be automatically made throughout the gathering cycle so that current and product temperature will remain within the desired operating range. Constant-pressure method offers the advantage that the product temperature and plasticity will remain at a near uniform level throughout the cycle.

"Controlled speed" of ram travel is an alternate method of operation. Applied secondary voltage remains constant throughout the cycle and heating current will then vary inversely with the work resistance. Velocity of ram travel is increased or decreased by varying the rate at which high-pressure oil is applied to the ram-operating piston.

Using the controlled-speed method it is possible to vary ram velocity by a cam-operated flow valve. If product temperature changes (due to conditions of the anvil insert or the clamp dies), over or underheating may occur since there is no means to automatically compensate for this change using the controlled-speed method.

Thus, "constant-pressure" method is recommended for mechanized operating cycles since a change in product temperature will cause a corresponding automatic change in ram velocity.

Description of a Gathering Machine

Figure 10 is a general-purpose, hydraulically-operated, vertical-type gathering machine. The ram with its operating cylinder, the retracting slide and the anvil are self contained in a box-type structure. The transformer and portions for conveying current to the anvil and clamp jaws are not shown since connection is made at the rear of the machine. The hydraulic pumping unit is located at the lower left and supplies high-pressure oil for operating the ram, clamp jaws and the retractable slide.

The rod is positioned in the machine with the end to be upset in contact with the anvil and it is gripped between the retractable clamp jaws. Pressure is then applied by the ram and current is automatically initiated. A direct relationship between ram movement and retraction of the clamp jaws is established by means of a cam-operated servo-mechanism. Cam "A" attached to the retractable slide and cam "B" on the ram are mechanically interconnected for operation of the servo-valve. Downward travel of the ram will move the operating piston in a direction that

Fig. 12 Control rod made from $^{3}/_{4}$-in. OD by $^{3}/_{64}$-in. wall tubing, illustrating various steps of forging and machining after the metal-gathering operation

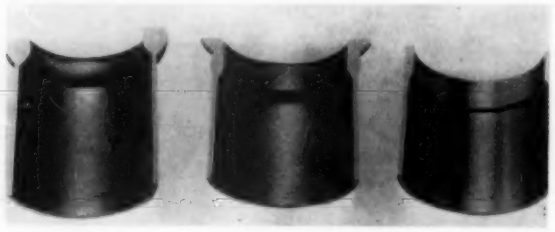

Fig. 13 Cross sections of tubing 3-in. OD by $^{1}/_{16}$-in. wall showing the gather, extrusion and machined condition

Fig. 14 A one-piece aircraft strut produced by metal gathering, forging and machining operations from $2^{1}/_{2}$-in. OD by $^{1}/_{5}$-in. wall tubing

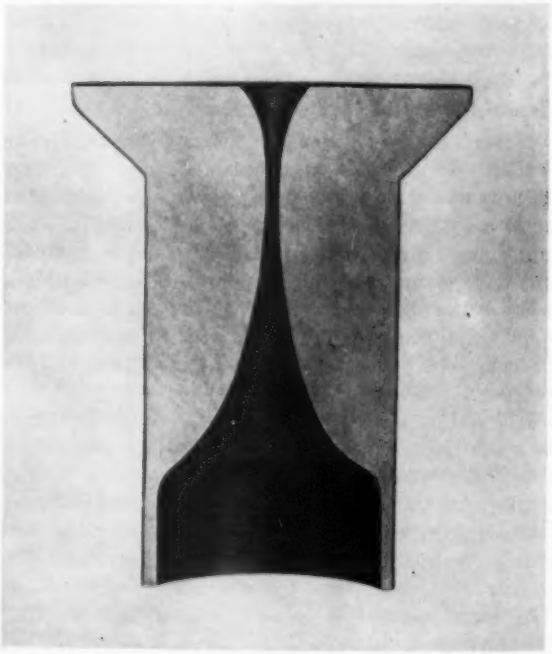

Fig. 15 Gathered and forged end, prior to machining operation, for aircraft strut shown in Fig. 14

permits oil flow to the retractable slide operating cylinder. This will cause the slide to elevate until cam "A" returns the operating piston to the neutral position. This principle of operation insures that retraction of the clamp jaws will always be a function of ram travel.

The cam-operated interconnection further enables moving the retractable slide at other than a linear rate with respect to ram travel. The occasion for this may be to retard slide movement at the start of the cycle until a specific upset base diameter has been established. It may also be desirable to change rate of slide movement during the cycle or when approaching the end to obtain the required formation. Shape of upset is consistently controlled by cam operation of the upset slide and is a convenience for setting up the machine on a particular job.

Cam "C" is for varying pressure applied to the upset ram. Increasing or decreasing pressure as required during work cycle will be according to cam contour.

Also attached to the ram is a rack for operating a drum-type switch used for initiating different heat levels during the cycle. Work resistance changes as the gathered formation grows, which means that the secondary voltage must be progressively altered if current is to remain at a uniform level.

Figure 11 is a control panel of specific design for resistance metal gathering. This particular panel has adjustment for initial heat level plus five additional levels identified by numbers 1 through 5. A manual heat adjustment is also provided which is of value for initial set-up purposes.

Instantaneous increase or decrease in voltage during the cycle will create a corresponding change in current

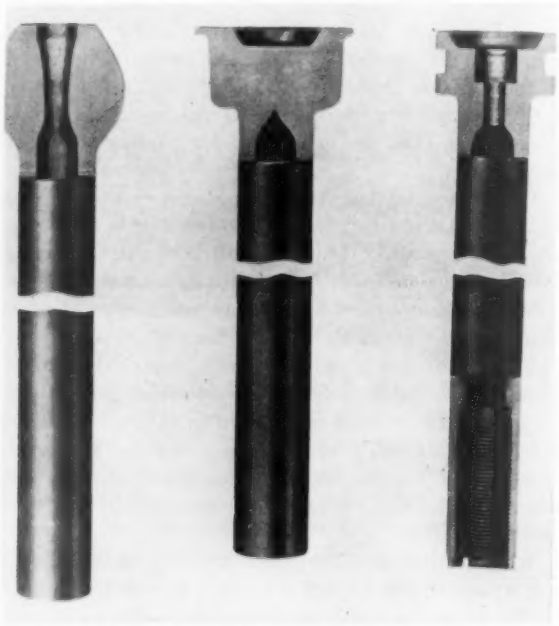

Fig. 16 Manufacture of hydraulic piston showing the gathered, forged and machined product

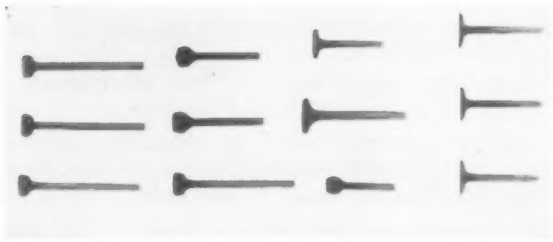

Fig. 17 Various types of end gatherings used in manufacture of poppet valves

and this may affect smoothness of the upset, depending on the material used. For this reason, slope control is added for each heat level so that the change can be made gradually depending on slope time adjustment.

Typical Applications for the Gathering Process

Termination of tubular products used for control rods and structural members in the aircraft industry frequently requires heavier wall thicknesses for internal or external threading. Other types of end termination are commonly employed that likewise require a heavier wall thickness.

The usual manufacturing procedure has been to weld or braze suitable end fittings to the tubular members. A second method is to use a tube of sufficient wall thickness so that reduction as required can be tolerated.

Resistance-gathering technique is employed to increase wall thickness of tubing for producing integral assemblies. Drop forging, machining, extruding and swagging are methods used to obtain the final shape, depending on individual requirements. Tubing up to 5 in. diam, wall thickness ranging from 0.031 to 0.500 in., alloy and stainless steel have been gathered using this process.

Figure 12 is the end of a control rod made from $^{3}/_{4}$-in. OD x $^{3}/_{16}$-in. wall tubing with the wall thickness increased by the gathering process preparatory for a subsequent threading operation. This illustrates the gathered end, the effect of forging and the final form obtained by a cutoff and drilling operation.

Figure 13 illustrates the principle of increasing wall thickness of tubing. These sections were cut from 3-in. OD x $^{1}/_{16}$-in. wall tubing, showing the gather, the end as extruded and the finished end machined to the required form.

The aircraft strut shown in Fig. 14 is a solid one-piece assembly made from $2^{1}/_{2}$-in. OD x $^{1}/_{8}$-in. wall Type 4140 steel tubing. Over 2 lin ft of tubing were fed into the gather on each end and forged into the shape shown by Fig. 15. The clevis ends were obtained by machining the gathered and forged ends for completion of an integral strut assembly.

Figure 16 is a hydraulic piston made from $1^{5}/_{16}$-in. OD x $^{1}/_{4}$-in. wall Type 4140 steel tubing. The successive cutaway views illustrate the original gathering, the forging and the final machined end.

Figure 17 shows type of end gathering for the manufacturing of poppet valves. The material is Silchrome

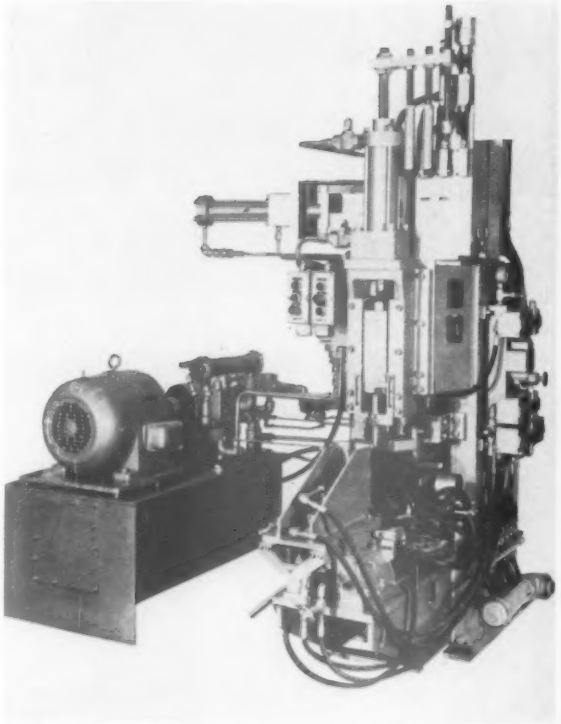

Fig. 18 Special metal-gathering machine for poppet valves with automatic loading and unloading features

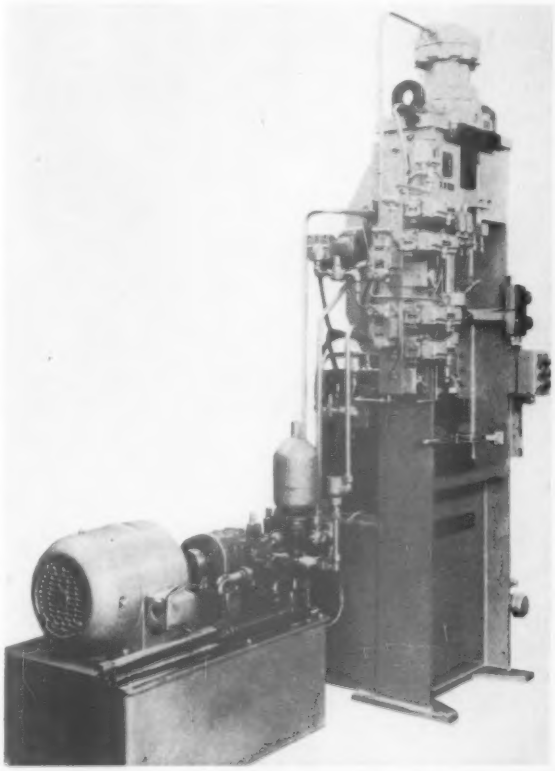

Fig. 19 Special machine for metal gathering of root section of turbine blade

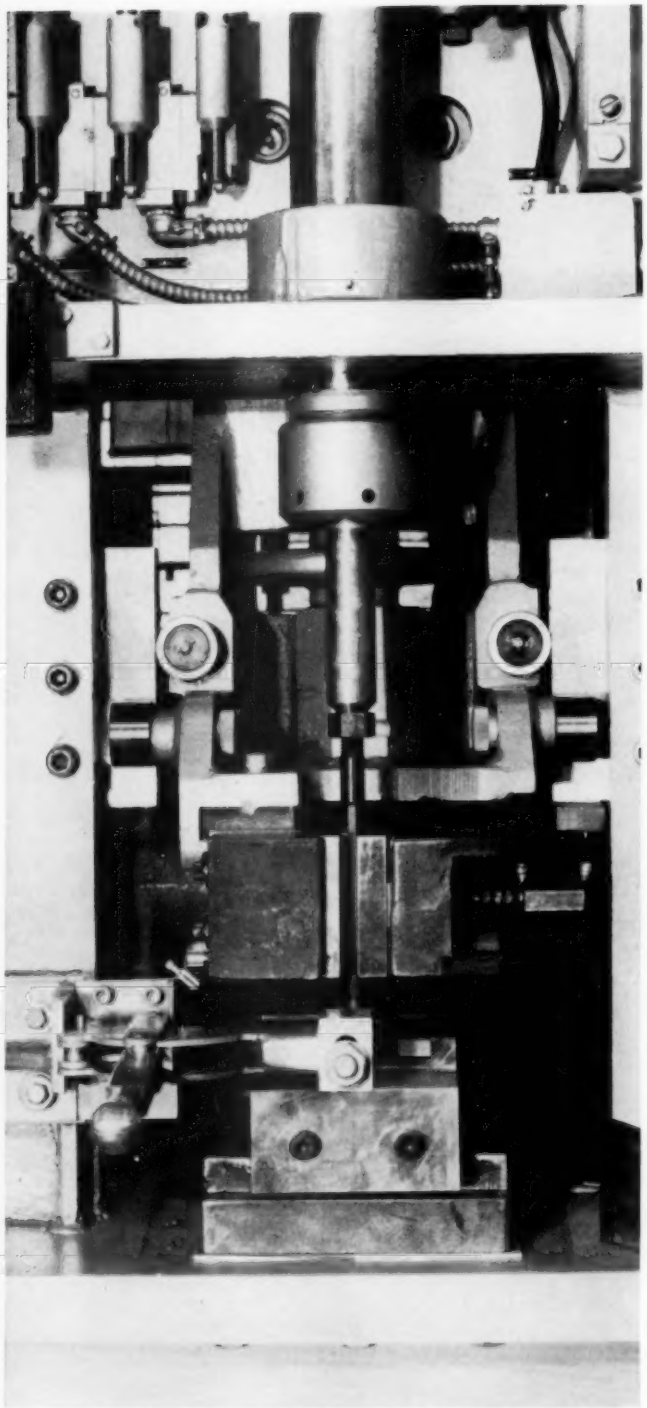

Fig. 20 Tooling arrangement in machine shown in Fig. 18

valve steel, approximately $^{7}/_{16}$-in. OD and ground to a smooth finish near the diameter of the finished stem. The gathered section is forged on a forge press to the final shape using the same heat acquired during the gathering operation.

Figure 18 is a gathering machine of special design for manufacturing automotive-intake-valve forging blanks. Rod material is automatically loaded and the transfer mechanism obscures the upsetting mechanism in this particular illustration. All material is hopper fed and forging blanks are automatically transferred to crank-type presses for the finish forging operation.

The machine illustrated by Fig. 19 gathers the end of a rectangular section that will ultimately be forged into a jet-engine turbine blade. Figure 20 shows the anvil, retracting jaws and the upsetting ram of the same machine. A gathered section as processed on this machine is shown at the lower right of this illustration.

The lead photograph at the beginning of this article illustrates other products produced by the resistance metal-gathering process and subsequent forging operations.

Advantages of Metal Gathering

Present-day manufacturing frequently requires use of special alloys that are difficult to forge because of strict limitations on the forging temperatures. This generally means that excessively high pressures are required, which increases wear on the forging dies. Many progressive steps are sometimes necessary, depending on the final shape. Resistance metal gathering can be used for assisting the required forming and it offers the advantage of decreasing the number of forging operations required. Forging die life is improved since metal gathering can create a form in close proximity to the required final shape.

Advantageous grain flow can be obtained by controlling shape of the gathered formation. For this reason many metallurgists believe that this improves such qualities as ductility and resistance to shock.

The volume of gathered formation can be precisely controlled so that simple shapes can be forged without subsequent trimming being required. This offers a material savings, and it eliminates handling cost for the trimming operation.

Furnaces normally used for heating materials to the forging temperature are not required since heating is combined with gathering. Heating is confined to the immediate working area which simplifies handling during forge operations and produces a formation free of scale that would affect life of the forging dies.

Gathering in some instances can be used to eliminate welding or brazing since an integral one-piece assembly may be obtained by this process. This offers a possible solution for an assembly made in two pieces that requires a 100% joint efficiency.

Present-day equipment can be provided with a completely automatic operating cycle, and the operator is only required to load and unload workpieces. Therefore, the process is suitable for application of automatic work-handling devices according to present-day manufacturing requirements.

Acknowledgments

To Thompson Products, Inc., Bell, Calif., the author gratefully extends acknowledgment for invaluable assistance, operating data, photos, etc.

HYDROGEN VS. ACETYLENE VS. INERT GAS IN WELDING ALUMINUM ALLOYS

Investigation concerned primarily with type, amount and location of porosity and fusion obtained with these three methods of welding

BY J. KOZIARSKI

ABSTRACT. It seems that the present outlook on these three methods of welding warrants a revision. They seem to have advantages and limitations depending on the required quality and manufacturing economy. Quality-wise and economy-wise, oxyacetylene welding of aluminum alloys seems, in general, to be superior to oxyhydrogen welding. Also, there is an indication that, in some cases, it may be superior to inert-gas welding, at least for gages met in an aircraft. The chemical composition of the welded alloy, and especially the magnesium content, seems to have a decisive effect on the amount of porosity and on fusion. The present consideration is primarily concerned with type, amount and location of porosity and fusion obtained with these three methods of welding. It would be interesting to explore the effect of the above on the strength and, especially, on the fatigue properties of aluminum alloys welded with these three methods.

Introduction

The oxyhydrogen, oxyacetylene and inert-gas tungsten-arc* welding processes have some advantages and limitations which should be taken into account when considering the desired end qualities of a fabricated product or its cost. However, the author feels that there exists a need for some revision of the present concept of weldability of aluminum and its alloys from the viewpoint of source of heat. Some of the problems will be outlined below under the two aspects: economic and quality.

All specimens used for experimenting were made from aircraft quality and aircraft gages of aluminum alloy sheets by a good production welder and under shop and not laboratory conditions. Facts given are those met in aircraft production; theoretical explanations are avoided unless they are self evident or essential.

Economic Aspects

Each cent saved at different steps of production may mean success or failure in today's highly competitive production.

It is difficult to talk about comparative cost between gas and inert-gas welding. As a matter of fact, in some cases such a comparison has no meaning as special design or service conditions may require inert-gas welding or necessitate the use of gas welding regardless of the relative cost. One, however, may easily compare the costs of oxyhydrogen with oxyacetylene welding. According to the author's experience, oxyacetylene welding is from 25 to 50% faster than oxyhydrogen welding. It is the high heat of combustion of the oxyacetylene flame (1433 Btu/ft³, dry, 30 in. Hg, 60° F as compared to 275 Btu for oxyhydrogen flame) which is responsible for this higher speed of welding rather than the 5660° F temperature of the former flame in comparison to 4820° F of the latter.[2] Otherwise speaking, with the same heat output the oxyacetylene flame has a smaller cross section than the oxyhydrogen flame, or, the same flame size gives higher heat output with the oxyacetylene flame than with the oxyhydrogen flame. Professor G. Ribaud[3] calls this "specific power" (puissance specifique). According to him, this specific power for the oxyacetylene flame is 9.5 Kcal/sec/cm²† as against 3.5 Kcal/sec./cm² for the oxyhydrogen flame.

The difference in the speed of welding between these two gases depends also to a great extent on the geometry of the joint. Long, straight joints give the highest difference.

The high heat concentration in the oxyacetylene flame is especially important in the speed of welding, because of the peculiar physical properties of aluminum. Specific heat (0.215 cal/g/°C at 20°C-vs. 0.11‡), heat of fusion (94.6 cal/g-vs. 65[4]), combined with its high thermal conductivity (0.53 cal/cm²/cm/°C/sec at 20°C-vs. 0.18[4]) produce a high heat demand in welding aluminum or its alloys. These high physical properties of aluminum, combined with its low melting point (660.2 ± 0.1°C-vs. 1539 ± 3[4]) makes aluminum

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* In future considerations the latter process will be referred to as inert-gas welding.

† Cm² means 1 sq cm section area of the flame at the torch tip end.

‡ Second values refer to iron and are given for comparative purposes only.

and its alloys more difficult to weld than, for instance, ferrous alloys. On the other hand, in welding aluminum alloy there exists the necessity of using a "harder" flame with oxyacetylene than with oxyhydrogen welding. Consequently it is easier to "burn through" thin aluminum alloy sections with the former than with the latter "softer" flame. This is due to the higher speed of flame propagation in the oxyacetylene flame which is 1350 cm/sec as against 890 cm/sec[2] in the case of the oxyhydrogen flame. As the result of this, higher gas pressure must be used in order to maintain higher gas speed in oxyacetylene ("hard" flame) than in oxyhydrogen torch ("soft" flame). However, suitable training of the welder easily overcomes these obstacles. On the other hand, the difficulties in setting up and controlling a neutral oxyhydrogen flame as compared to the ease of doing so in oxyacetylene flame, soon convinces a reluctant welder that the latter method of welding is at least as easy as the former.

The inert-gas process gives the highest speed among the three discussed methods of welding. This is especially true with thicker sections. Besides, inert-gas welding does not require the use of fluxes, which are not only expensive but their application and subsequent cleaning are time consuming. However, as pointed out above, it is difficult to make any general comparison of costs among inert-gas, oxyhydrogen and oxyacetylene welding, as there are other factors to be considered. These factors may make the use of one or the other method of welding mandatory.

Quality Aspects

1. *General*

There have been some references in the international literature to the effect of the speed of welding on such phenomena as intergranular precipitation, sensitivity to cracking, corrosion, stress-corrosion, fatigue, etc., in welding aluminum and its alloys. All these properties are adversely affected by a low speed of welding.

This paper will deal with some aspects of two problems: porosity in the heat-affected zone and fusion and their possible effect on properties of the welded joint made by different methods.

2. *Porosity*

The international literature contains a considerable number of papers concerning porosity in aluminum alloys and its mechanism of formation, prevention or reduction. There also exists a limited number of reports on porosity in aluminum alloy welds. Unfortunately, the literature concerning the amount or location of porosity in the weld, its dependence on the method of welding, or the effect of porosity on physical or strength properties, is almost nonexistent.[1]

Figures 1 through 13 summarize some experiences with porosity in aluminum and aluminum alloy welds. Aluminum sheets used for this purpose were of 0.062 to 0.064 in. thick. Backing in inert-gas welding was obtained with a grooved copper plate. No inert gas was circulated in the groove. Strips cut from sheets

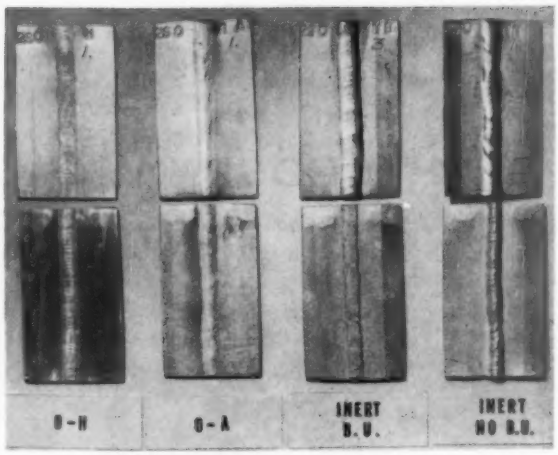

Fig. 1 1100-O aluminum-sheet welded with oxyhydrogen, oxyacetylene and inert gas (with and without backing). (Strips cut from the sheet were used as filler metal)

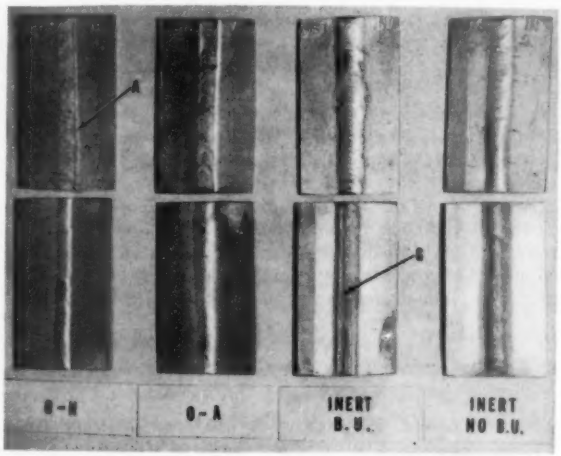

Fig. 2 6061-T6 aluminum-alloy sheet. (Welded as outlined under Fig. 1)

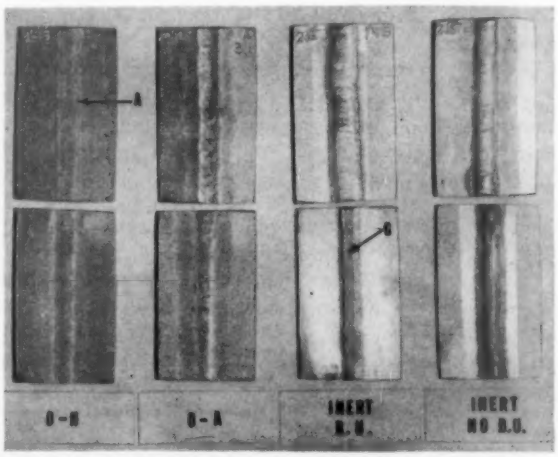

Fig. 3 2024-T3 aluminum-alloy sheet. (Welded as outlined under Fig. 1)

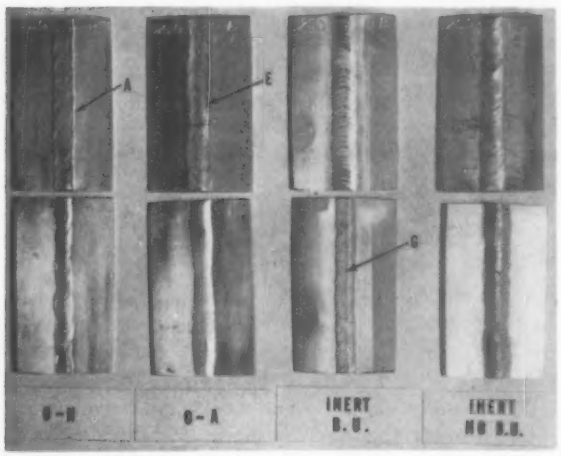

Fig. 4 5052-O aluminum-alloy sheet. (Welded as outlined under Fig. 1)

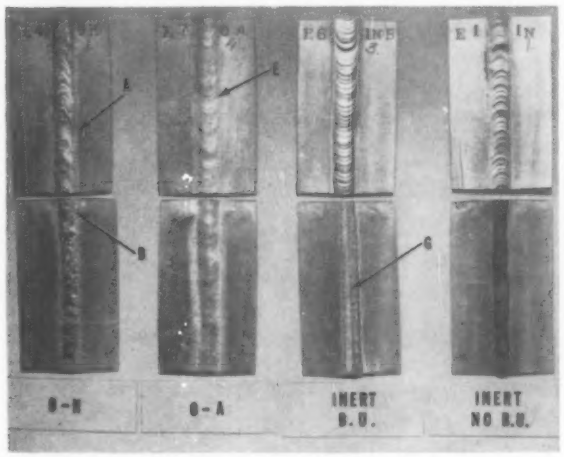

Fig. 5 5083-O aluminum-alloy sheet. (Welded as outlined under Fig. 1)

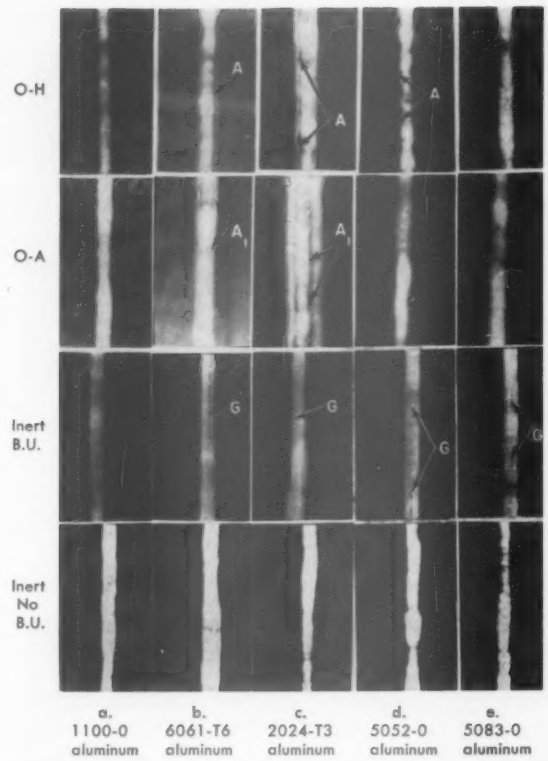

Fig. 6 X-ray of welded aluminum sheet from Figs. 1 through 5

(a) 1100-0 aluminum from Fig. 1. No insufficient fusion or porosity can be seen. (b) 6061-T6 aluminum—from Fig. 2. *Note:* Porosity in the heat-affected zone of the gas-welded specimen (*A* and *A*,) and insufficient fusion in the specimen welded by inert-gas process, with backing (*G*). (c) 2024-T3 aluminum—from Fig. 3. *Note:* Porosity condition indicated similar to (*b*). (*d*) 5052-0 aluminum—from Fig. 4. *Note:* Porosity condition indicated similar to (*b*). (*e*) 5083-0 aluminum—from Fig. 5. Note the absence of porosity in spite of high-magnesium content and the presence of insufficient fusion in the inert-gas welded with backing specimen.

were used as filler metal. Selected alloys contained increasing amounts of magnesium as follows:

Alloy	1100	6061	2024	5052	5083
Nominal Mg, %	0	1.0	1.5	2.5	4.45

In Figs. 1 through 5, welded specimens which were used for experimenting are shown. Upper rows represent the face side of welds, and the lower rows show the root side.

In Fig. 6 there are shown X-ray pictures; and in Figs. 7 through 12 macro photographs of sections across same welds are given.

For the ease of orientation, the photographs in Figs. 7 through 12 have been arranged in such a manner that every specimen is shown face up. The upper specimens show oxyhydrogen welds; the middle, oxyacetylene deposits; and the lower, inert-gas beads made with backing strip.

Oxyhydrogen Welding

Magnesium-containing aluminum alloys welded with oxyhydrogen flame show considerable porosity in the weld. It is especially severe along the heat-affected zone. The amount of porosity seems to increase with the increase of magnesium content (with the apparent exception of 5083 alloy). It is concentrated toward the face surface and diminishes toward the opposite root face (see *A*, Figs. 2 to 4; *A*, Figs. 6, *b* through *d*; *A*, Figs. 8 through 11 and *F*, Fig. 12). Heat-affected-zone porosity has roughly a triangular shape in oxyhydrogen welds and is irregular in oxyacetylene welds (see Figs. 8 through 10). As the result of porosity, the heat-affected zone on the face side shows swelling (see *F*, Figs. 8 through 10 and 12) and blistering (see *A*, Figs. 2 to 5 and 8 through 10 and 12).* As a rule, blistering and swelling are present on the face side, but sometimes may also be visible on the root side of the weld (see *B*, Fig. 5).

The heat-affected zone of 1100 aluminum does not

* In the case of 5083 aluminum blisters are rather fine and with very thin envelopes (*A*, Figs. 5 and 11). These envelopes are easily destroyed during preparation of specimens.

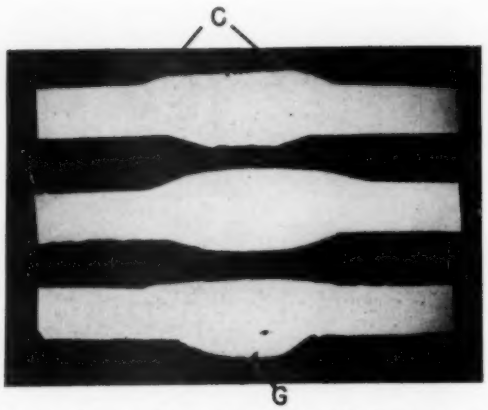

Fig. 7 Cross sections of welded 1100-0 aluminum-alloy sheet from Figs. 1 and 6a

Note some swelling (C) in the heat-affected zone in oxyhydrogen specimen and insufficient penetration (G) in inert-gas-welded specimen.

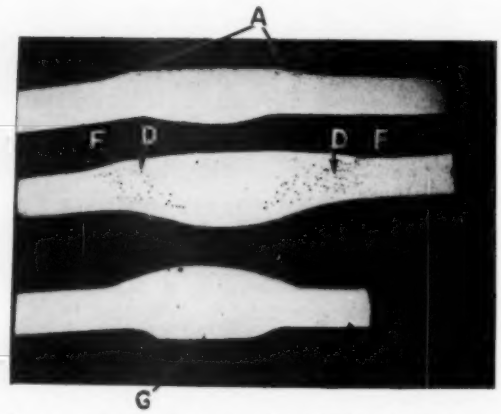

Fig. 8 Cross sections of welded 6061-T6 aluminum-alloy sheet from Figs. 2 and 6b

Note porosity concentrated toward the surface and blistering in oxyhydrogen welded specimen (A), porosity confined to the center (D) of specimen and swelling (F) in oxyacetylene welded specimen and insufficient fusion (G) in the inert-gas-welded specimen.

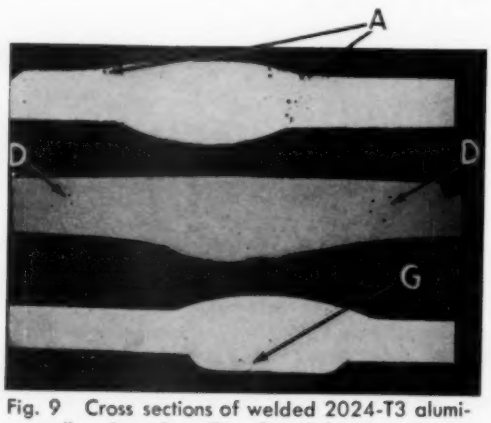

Fig. 9 Cross sections of welded 2024-T3 aluminum-alloy sheet from Figs. 3 and 6c

Note porosity condition indicated under Fig. 8, except that there is no swelling visible.

seem to contain any macroporosity. However, some swelling is apparent (see *C*, Fig. 7). This ordinarily would indicate that there was some microporosity, but it may not be discernible even under high magnification.

As mentioned above, 5083 aluminum alloy does not seem to follow the general trend in increasing porosity with the increase of magnesium content. This may be due to its chemical balance.

Oxyacetylene Welding

In oxyacetylene welds the amount of porosity seems to follow the trend of oxyhydrogen welds (see Figs. 1 to 12). However, there are also significant differences:

1. Generally, oxyacetylene welds contain much less macroporosity than oxyhydrogen welds. This is especially visible in X-ray photographs (Fig. 6).

2. Porosity is always concentrated toward the center of the specimen (see *D*, Figs. 8, 9 and 12).

3. As a rule, blistering is absent. Instead, some wrinkling may be observed in the heat-affected zone of alloys with higher magnesium content (see *E*, Figs. 4 and 5).

4. There is some swelling in the heat-affected zone (see Figs. 8 and 12) even in the absence of macroporosity (see *F*, Fig. 10). This would indicate that there might be some microporosity present.

5. Again, there is no blistering, porosity or swelling with 1100 aluminum. Figures 8 and 12 show extreme cases of macroporosity in 6061-T6 and 5052-0 aluminum alloy respectively, welded with oxyacetylene. However, it is definitely concentrated toward the center of the specimens.

Source of Porosity in Welding

Porosity in aluminum castings is very common. Welding is a sort of die casting on a miniature scale, with the weld metal being somewhat attached to the die. Eboral, Ransley, Parker, Neufeld, Rowe, Ennor, Eastwood and others made valuable contributions to this problem. Some papers on the subject which may be of interest to welding circles are listed in Reference 1. However, research work by the British Non-Ferrous Metals Research Assn. is of a direct interest to our discussion.[5,6] Whitaker, when discussing porosity in aluminum-magnesium castings, states:

1. When the alloy is poured its surface is oxidized, and hydrogen separated from the moisture is absorbed into the casting. This is especially violent between solidus and liquidus. Absorbed hydrogen is subsequently liberated during solidification and produces porosity.

2. The amount of porosity increases with an increase in magnesium content and with the time length of solidification.

3. There is more porosity in heavy than in thin sections.

4. The highest amount of porosity is concentrated toward the surface and decreases toward the center of the section.

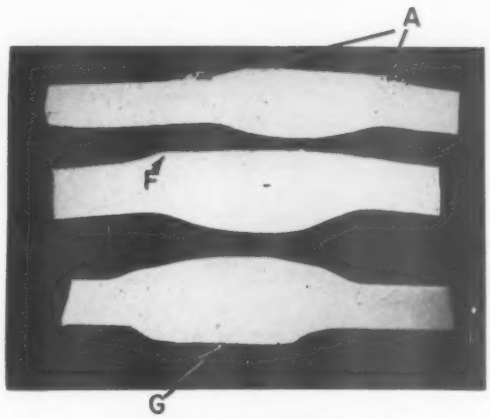

Fig. 10 Cross sections of welded 5052-0 aluminum-alloy sheet from Figs. 4 and 6*d*

Note excessive porosity concentrated toward the surface and blistering on the oxyhydrogen-welded specimen (*A*), and absence of porosity but considerable swelling in the oxyacetylene-welded specimen (*F*) and insufficient fusion in the inert-gas-welded specimen (*G*).

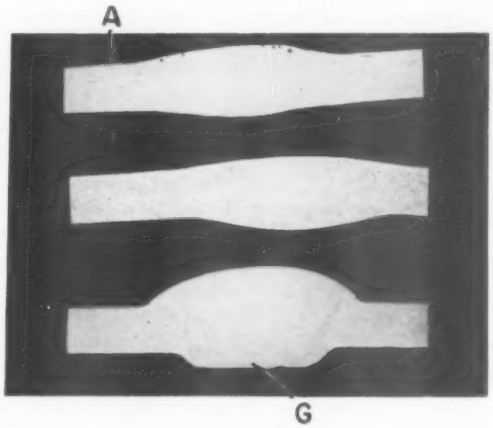

Fig. 11 Cross section of welded 5083-0 aluminum-alloy sheet from Figs. 5 and 6*e*

Note porosity in the weld metal, very fine blisters (*A*) in the oxyhydrogen-welded specimens and insufficient fusion (*G*) in the inert-gas-welded specimen.

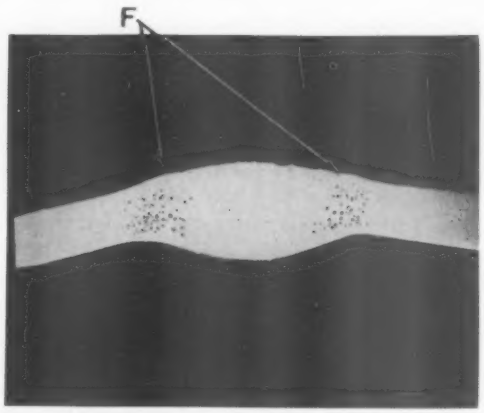

Fig. 12 An extreme case of heavy porosity in 5052-0 aluminum alloy welded with oxyacetylene flame

Note porosity confined to the center of the specimen.

Mechanism of Porosity Formation

Some research workers have tried to explain the mechanism of hydrogen porosity formation in aluminum and especially in aluminum alloys containing magnesium.[5, 7–15] An attempt at summarizing these opinions has been made in Reference 1. In brief, it looks as follows:

Magnesium possesses a high chemical affinity for oxygen. It decomposes water even at room temperature and releases free atomic hydrogen. When an aluminum alloy containing magnesium is heated, magnesium diffuses toward the surface and dissociates water. As a result of this, the free atomic (in nascent state) hydrogen exercises a very high partial pressure and is dissolved in the alloy. The higher the temperature, the more violent is the reaction and the more hydrogen is dissolved. On cooling, atomic hydrogen is thrown out of solution and either evolves or precipitates as molecular hydrogen in the metal. The slower the cooling, the more hydrogen is thrown out of solution and the more porosity is formed.

The problem of porosity in welding aluminum alloys is rather complex because of the number of parameters involved: the magnesium content, the temperature of the weld, the section size of welded part, the speed of welding and the nature of welding atmosphere.

The former three have already previously been mentioned. However, the latter two require some particular attention.

1. Speed of Welding. As has been stated previously, in the welding of aircraft-gage aluminum the speed of deposition is 25 to 50% higher with oxyacetylene than with oxyhydrogen flame. Consequently, hydrogen has more time to be formed and dissolved during heating and to be precipitated during cooling when welding with the oxyhydrogen than with the oxyacetylene flame. It is, therefore, not surprising that the former yields more porosity than the latter.

2. Nature of Welding Atmosphere. Simplified chemical reactions of the oxyhydrogen and the oxyacetylene flame are depicted in Fig. 13.

As may be seen, the final combustion products in the oxyhydrogen flame are composed 100% of water vapor, while oxyacetylene flame yields only $^1/_3$ volume of water vapor.

In addition, the oxyacetylene flame has much higher reducing properties than the oxyhydrogen flame. Consequently, there is less chance of oxidation (and porosity) with the former than with the latter. The higher speed of welding, the lower water-vapor content in the combustion products and the higher reducing properties of the oxyacetylene flame than those of the oxyhydrogen flame are the reasons why welding with the former flame gives less porosity in aluminum alloys than with the latter.

Interesting Questions

Two interesting questions may be raised after analyzing Figs. 1 through 12:

1. Why is porosity concentrated mostly in the heat-affected zone?

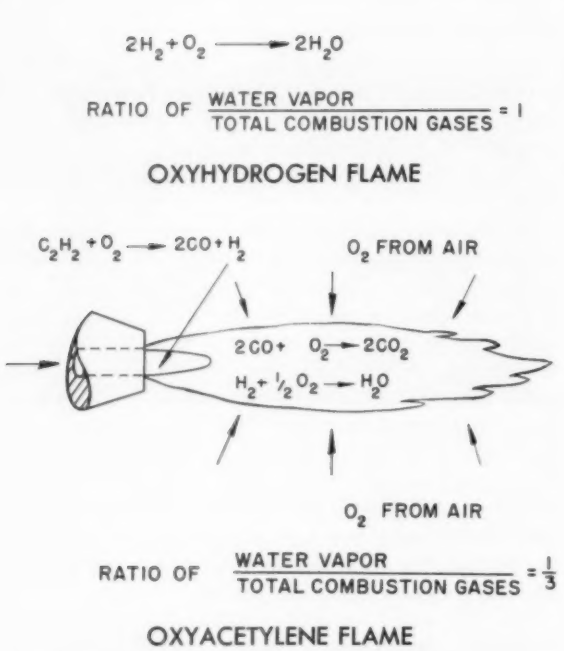
$2H_2+O_2 \longrightarrow 2H_2O$
RATIO OF WATER VAPOR / TOTAL COMBUSTION GASES = 1
OXYHYDROGEN FLAME
$C_2H_2+O_2 \longrightarrow 2CO+H_2$
O_2 FROM AIR
$2CO+O_2 \longrightarrow 2CO_2$
$H_2+\frac{1}{2}O_2 \longrightarrow H_2O$
O_2 FROM AIR
RATIO OF WATER VAPOR / TOTAL COMBUSTION GASES = 1/3
OXYACETYLENE FLAME

Fig. 13 Simplified reaction in oxyhydrogen and oxyacetylene flames

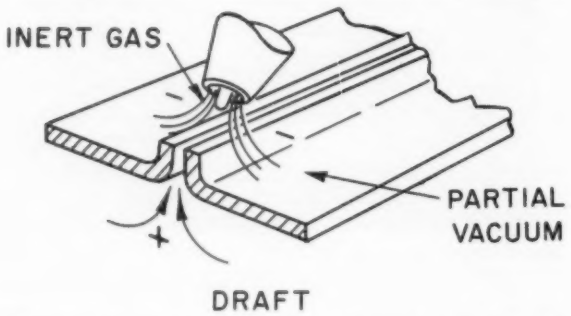

Fig. 14 Showing why draft produced by differential temperature and pressure may be the source of porosity and insufficient fusion in inert-gas welding

2. Why, in an oxyhydrogen weld, is porosity concentrated similarly to casting, i.e., close to the face surface, while in the oxyacetylene weld, it is confined to the center of the specimen?

The author prefers not to attempt to give the answers to these questions because any answer would be almost a pure speculation without knowing the equilibrium conditions existing at a given moment.

Porosity in Inert-Gas Welding

If the previous reasoning is right, inert-gas welding should give a porosity-free weld as no water vapor is generated during the welding of aluminum alloys. However, this does not seem to be true. Microscopic examination seems to reveal some microporosity concentrated on the root side of the weld.[1] An attempt to explain this will be made later.

This paper is primarily concerned with speed of welding, porosity and fusion. However, one important advantage of inert-gas welding over gas welding must be kept in mind. The former produces welds with less metal and heat-affected zone cracking than the latter two processes.[1] This factor quite often may dictate the use of inert-gas welding, especially in thicker sections.

3. *Fusion*

One may see that fusion is good in gas welding (Figs. 1 through 11) whether aluminum is welded with oxyhydrogen or with oxyacetylene. This, however, does not seem to be true with inert-gas welding. Root side of welds made with inert-gas with back-up almost always shows a distinct line running along the middle of the root (see *G*, Figs. 2 to 5). X-ray pictures reveal some voids along the middle of the weld (see *G*, Figs. 6*b* through *e*). Sections of inert-gas welds confirm the existence of insufficient fusion (see *G*, Figs. 7 through 11). The severity of this lack of fusion seems also to increase with the increase of magnesium content. Even the commercially pure aluminum (1100) does not seem to be completely free from this defect although it does not contain magnesium (see Fig. 7).

The explanation of the lack of fusion seems to be simple. In the case of gas welding, fluxes are used which produce a liquid protective film around edges to be welded. This film probably provides some isolation from the air and water vapor. In the case of the inert-gas welding, however, the gas provides protection to the face of the weld only. The root side has less of an inert-gas envelope. Consequently, air and water vapor have relatively easy access to the root even if the weld is backed, but without a flow of inert gas between the backing plate and the weld (Fig. 14). The edges on the root side are easily oxidized. In addition, a draft caused by the pressure differential, which, in turn, is produced by the temperature differential probably aggravates this situation. It is also probable that moisture not only has a free access to the root but is carried by the draft into the inert-gas envelope. There, it would be dissociated in the high arc temperature; oxygen would oxidize and hydrogen would be dissolved.

As is known, aluminum and magnesium oxidize in air and moisture, producing refractory oxides which have a very high melting point (2050°C or 3722° F for Al_2O_3 and 2800°C or 5072° F for MgO)[16] while melting point of aluminum and magnesium is comparatively low (1220.4 ± 0.2° F and 1202 + 4° F respectively).[4] At some elevated temperatures, a very tenacious mixture of these two oxides is formed on the surface. Therefore, it is not surprising that an aluminum surface, covered with such films which do not melt during welding, cannot be fused together.*

The action of the draft seems to be especially detrimental in flange welding. Figure 15 shows a few flange welds between 6061-T6 bosses and 5052-0 sheet. They

* It is interesting to note that welds made with inert gas but without backing up do not show this insufficient fusion. Is the draft in the channel between backing up plate and the weld responsible for this?

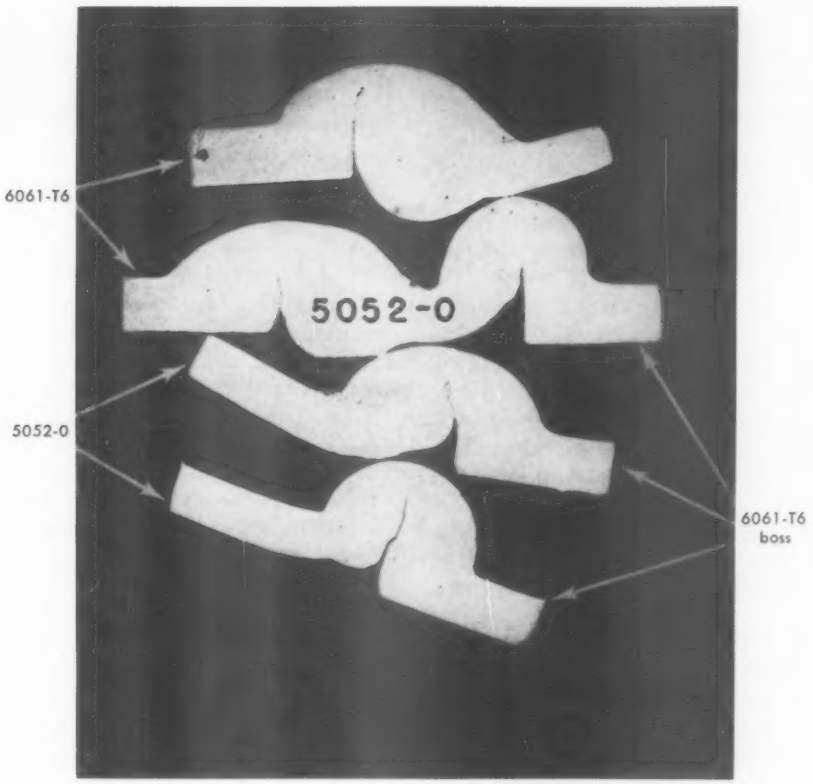

Fig. 15 Insufficient fusion in welds between 6061-T6 boss and 5052-0 sheet

are production specimens. Some welds show considerable indication of insufficient fusion (lack of fusion).

It seems that some sort of noncorrosive and easily removable fluxes, which could be applied to the root side of the weld would be the most practical method of preventing possible hydrogen absorption and oxidation of the weld at its root side. The use of the inert gas for this purpose seems to be impractical in aircraft production, unless one would be willing to go into very complicated and expensive tooling.

4. *Effect of Porosity and Insufficient Fusion*

Static and Dynamic Properties

Apparently, there are no data available showing the strength effect of porosity and/or insufficient fusion of welded aluminum joints. Some authors[8,11, 12, 14] point out, however, that porosity has an adverse effect on tensile and yield strength, and on impact.

One may, however, expect that fatigue strength would especially be affected by porosity[8] and insufficient fusion. The type of porosity found in oxyhydrogen welds (Figs. 8 through 12) is probably more harmful than that met in oxyacetylene welds. This would be because surface porosity in the former would produce a greater notch effect than in the latter. One may also expect that flexural fatigue strength in oxyhydrogen welds would be affected more than axial fatigue.

The decrease of static and dynamic properties may be due not only to the notch effect and to the decreased section, but also to residual stresses produced by gas pressure in the voids.

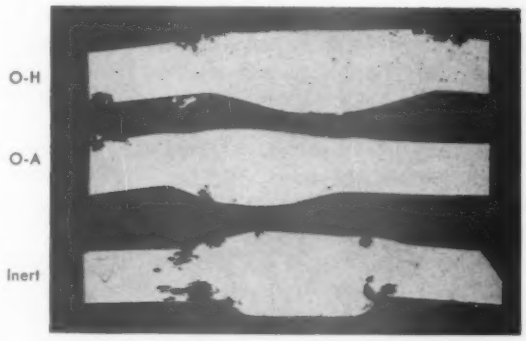

Fig. 16 5052-0 welded aluminum alloy after corrosion test

312 hr in 7% NaCl in H_2O + 3% H_2O_2; solution changed daily. Note that the inert-gas-welded specimen was attacked more severely than the others. Oxyhydrogen-welded specimen was next in severity, and the oxyacetylene specimen was the least attacked.

It would be desirable to have some experimental, static and fatigue data of welds containing different amount and differently located porosity and insufficient fusion.

It has been found that porosity in oxyhydrogen welded aluminum sheet occasionally produces the lack of tightness which cannot be neglected in the case of tanks or pressure vessels.

Corrosion

A very limited number of experiments run in 7% aqueous solution of NaCl + 3% H_2O_2† seem to indicate that porosity and its location have considerable effect on resistance to corrosion (Fig. 16). The specimens used were made from 5052-0 aluminum alloy and were kept in the solution for 312 hr. Solution was changed daily. On the basis of the results obtained, inert-gas welded 5052-0 specimens seem to be the most severely attacked, followed in the severity of attack by oxyhydrogen and oxyacetylene welded specimens. The heat-affected zone of the inert-gas and oxyhydrogen welds is attacked the most, while the heat-affected zone of oxyacetylene weld is almost corrosion free. The corrosion attack in the oxyhydrogen weld seems to be of the concentration cell corrosion type. This would be understandable. Why, however, is the inert-gas welded joint the most severely attacked of the three? Is microporosity as previously observed by the author[1] responsible for this? In this case, the strongest corrosive attack on the root side would be explainable.

A similar gradation of corrosion has been observed in other welded aluminum alloys. However, the attack in 2024-T3 alloy was rather uniform surface attack and not by pitting. There also was some intergranular corrosion in the heat-affected zone. Oxyhydrogen welded specimens, as could be expected, were the most affected while inert-gas welded ones showed the least intergranular attack. Consequently, the intergranular precipitation of $CuAl_2$ cannot only be blamed for this attack as, in such a case, the heat-affected zone should be the most strongly affected, not only by intergranular corrosion but by pitting as well.

Conclusions

On the basis of the above discussion, the following conclusions may be drawn.

1. Quality-wise and economy-wise, oxyacetylene welding seems generally to have undisputable superiority over oxyhydrogen welding of aluminum alloys.

2. Very thin sections (about 0.025 in. and below) may be easier to weld with oxyhydrogen flame than with oxyacetylene flame. Also, small-radius circular welds on thin sections seem to give less cracking with oxyhydrogen than with oxyacetylene flame.

3. Oxyacetylene welding, in certain cases, seems also to have superiority over inert-gas welding.

4. The speed of welding and the lack of the use of fluxes are the two main advantages of inert-gas welding over gas welding.

† One may contest the wisdom of using such corrosive media, especially the addition of such a powerful oxidizing agent as hydrogen peroxide. However, the tests in question were intended to be comparative and qualitative, and not quantitative.

Acknowledgment

The author wishes to express his thanks to the management of the Vertol Aircraft Corp. and, in particular, to L. L. Douglas, Vice-President, Engineering, for making available company facilities in preparation of this paper and to Kaiser Aluminum and Chemical Sales, Inc., for supplying some of the material cost free. He also wishes to express his gratitude to all those members of the Vertol Aircraft Corp. who so kindly helped in accomplishing his work. In particular he wishes to thank Mrs. B. Abbott, Miss S. Mahoney and C. Dahl, R. Goodier, S. S. Kingsbury, J. McKiernan, M. Metzler, H. J. Mulvey, C. L. Ritchie, L. Russo, N. Stickler, A. Vincius and E. E. Weismantal.

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HIGH SPEED PLUS HIGH QUALITY

Fabricating shop maintains high-speed production of high-quality products with mechanized submerged-arc welding

Mechanized submerged-arc welding plays an important part in the efficient production of boilers and heat exchangers at the Henry Vogt Machine Co., Louisville, Ky. The modern, submerged-arc welding machines produce sound, high-strength, welded joints with an extremely low weld repair incidence which helps the manufacturer maintain his high productive capacity.

Because the submerged-arc-welded products in this plant vary so greatly in size and shape, special rotating and traveling fixtures had to be built to meet their requirements. Figure 1 shows one of the submerged-arc welding installations which is used to handle the smaller, cylindrical parts. The fixture rollers revolve the part for circumferential welding or, if a longitudinal weld is made, the part is held stationary while the submerged-arc welding machine is set in motion by means of the submerged-arc welding side beam carriage. The object being welded in Fig. 1 consists of two, dome-shaped parts held temporarily together by a sleeve (see Fig. 2). The sleeve is used as an aid in aligning the parts and is removed after welding. Welding speed is 16 ipm on this oil chiller component which is fabricated from $^3/_8$-in.-thick carbon steel plate.

Based on a story by the Linde Air Products Co., New York, N. Y.

A second, high-production submerged-arc welding installation is illustrated in Fig. 3. The design of this fixture allows the submerged-arc welding machine to be adjusted for height and also swung out from the wall to accommodate the welding of shells and tanks of various diameters. The welding head is mounted on a

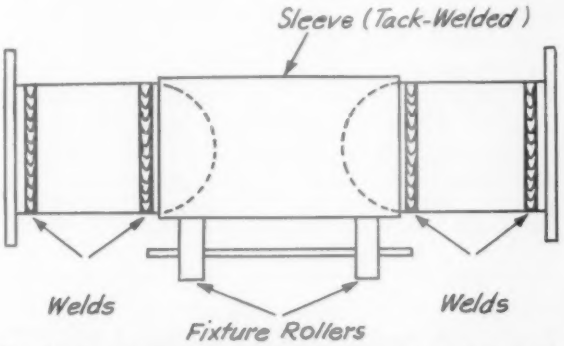

Fig. 2 Two domed heat exchanger parts are held in position for welding by a sleeve as shown above. This technique simplifies alignment and provides a "true" bearing surface for the fixture rollers

Fig. 1 High-speed welding of small diameter boiler and heat exchanger parts is done in this submerged-arc installation. A submerged-arc welding head, mounted on a side beam carriage is welding this part at 16 ipm

Fig. 3 A submerged-arc welding head mounted on a machine carriage forms the basic parts of this welding fixture. A sound, clean weld is being made at 8 ipm on this surge drum fabricated from $^1/_2$-in.-thick carbon steel

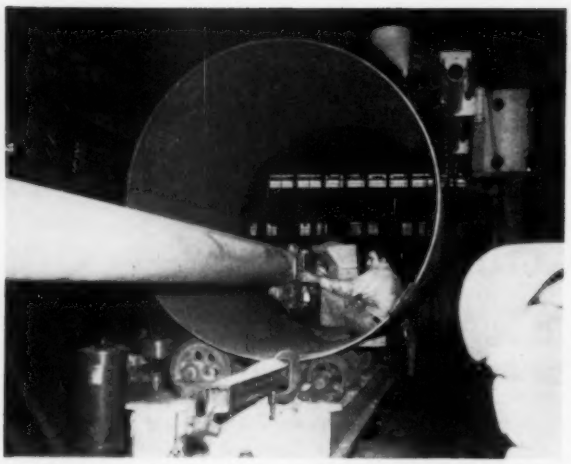

Fig. 4 When large shells are to be welded, this boom-mounted submerged-arc welding head is brought into action. When making longitudinal welds, the necessary motion is provided by the motorized "railway" car

Fig. 5 All travel and welding controls are mounted on the boom for ease of operation. Welding speed on this 7/16-in.-thick carbon-steel shell is 7 ipm. A second weld from the outside will be made to complete this joint

machine carriage to provide travel for making longitudinal welds. The part being welded in Fig. 3 is a surge drum from 1/2-in.-thick carbon-steel plate. Welding speed is 8 ipm.

A third submerged-arc welding machine, mounted on a long boom, is installed in the same area of this shop for making longitudinal and circumferential welds inside large boiler and heat exchanger shells (see Figs. 4 and 5). During welding, the submerged-arc welding machine and boom remain stationary as the necessary travel is provided by a motorized "railway" car. For ease of operation, all welding and travel controls are mounted on the boom next to the welding head. Welding speed on this 7/16-in.-thick carbon-steel heat exchanger shell is 7 ipm. Backing composition is held in a trough underneath the weld to support the molten weld metal, particularly when there are gaps in the joint.

CO_2 AND ARGON USED ON DIFFERENT SEAMS FOR HIGH-VOLUME WELDING

BY F. K. MACK

Fig. 1 Automatic welding head moves along the seam of the shell held in a fixture with water-cooled backing bar

The Parish Pressed Steel Division of Dana Corp., Reading, Pa., manufactures an almost endless variety of stampings and defense items.

Current high production with the gas-shielded metal-arc welding process at this plant points up the welding values obtained from efficient use of two different shielding gases on the same project.

Parish uses CO_2 as the shielding medium for the circumferential seams on mild-steel practice bombs for the U. S. Army, while argon is used for the longitudinal seams on these same bombs.

The reason for this is that the circumferential or girth seams are ideally suited for the greater penetrating characteristics of CO_2 shielding. Because of the rougher nature of the arc in carbon dioxide, it is usually used where short arc lengths and deep penetration are satisfactory. On the other hand, the inherent stability of the argon arc permits wide variation in arc length

F. K. Mack, Process Representative, Philadelphia District, Airco Sales.

Fig. 2 The longitudinal seam made with argon has been completed on the body of the shell

Fig. 3 Bulkhead, body and nose of the bomb are assembled at one work point, prior to the circumferential welds

used on the longitudinal seam where deep penetration is not desired.

The welding production cycle on the practice bombs (see Figs. 1 through 5) starts with the longitudinal seam of the body shell, which is 12 in. in diameter and $14^1/_2$ in. long. The material is SAE 1010 steel, 0.075 in. thick. Using mild-steel wire within a gas shield of 70 cfh argon and 2 cfh oxygen, a welding speed of 64 ipm on this seam is obtained.

Prior to the circumferential welds, the bulkhead, body, and nose of the bomb are assembled at one work point.

The two circumferential welds, using CO_2 as the shielding gas, are made simultaneously by two automatic welding heads. Welding speed, with the seams rotating under the fixed nozzles of the two welding heads, is 112 ipm. The required weld has a $^1/_{32}$-in. reinforcement. The mild-steel wire has sufficient quantities of manganese for strength and silicon for weld soundness to overcome the oxidizing effects of a CO_2 arc.

Thus, by versatile use of CO_2 and argon on appropriate seams, faster welding and quality of result are achieved at maximum economies on this high-volume welding project.

Fig. 4 Bomb assembly ready to be placed on rotating fixture for the circumferential welds

Fig. 5 Bomb assembly ready for welding of circumferential seams using CO_2 as shielding gas

MECHANIZED TUNGSTEN-ARC WELDING IS VITAL PART OF HIGH-PRODUCTION PLANT

Pastushin Aviation Corporation recently released details on their new Los Angeles jettisonable fuel tank plant. Built for maximum production with minimum effort, the company utilizes modern fabrication and handling methods to obtain a production rate of one 200-gal fuel tank every four minutes (see Fig. 1).

Mechanized inert-gas-shielded tungsten-arc welding is a vital part of this high-production plant, for virtually all the welding on these aluminum fuel tanks is done with this modern argon-gas-shielded welding method. Figure 2 shows one of the many tungsten-arc welding installations in this plant. The longitudinal joint on this fuel tank center section is being welded at 68 ipm. Material is 3S aluminum, 0.64 in. thick. Another welding installation is illustrated in Fig. 3. This "fixture" is being used to weld a spun aluminum nose cap to a previously welded conical nose section at 48 ipm. A $^1/_8$-in. standing flange has been used very successfully on this operation and has recently eliminated the use of cold-wire feed equipment.

Before the nose and center sections of the fuel tank are joined, extruded aluminum rings are fillet-welded to the joint edges as shown in Fig. 4. After this operation, the nose section and center section are tungsten-arc welded (see Fig. 5). Then, the tail section is joined by mechanical means and the assembly is complete except for adding hardware, cleaning and crating.

Based on a story by the Linde Air Products Co., New York, N. Y.

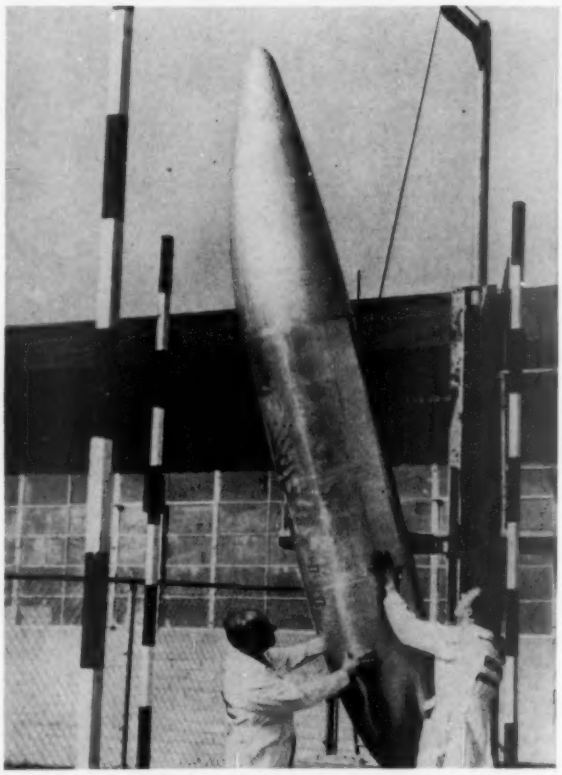

Fig. 1 One of these streamlined, all-aluminum, jettisonable fuel tanks is produced every four minutes with the help of mechanized inert-gas-shielded tungsten-arc welding installations

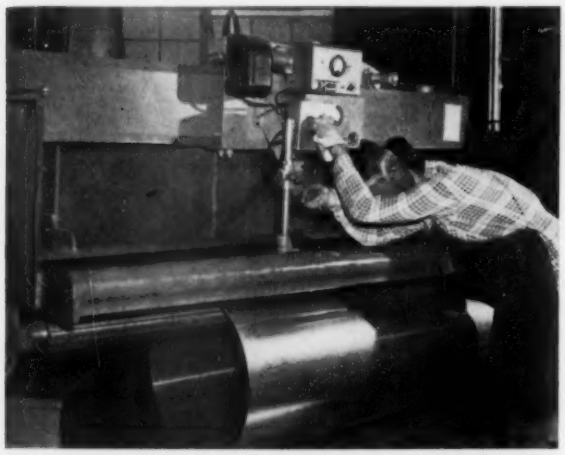

Fig. 2 The longitudinal joint in this fuel tank center section is being tungsten-arc welded at 68 ipm. Material is 3S aluminum, 0.064 in. in thickness.

Fig. 3 A weld joining a spun aluminum nose cap to a conical nose section is made in this fixture. Cold wire feed equipment on this operation has been recently eliminated by making a $^1/_8$-in. standing flange on the joint edges

Fig. 4 Before the fuel tank sections are welded together, an extruded aluminum ring is fillet-welded to the edge of the nose and center parts in this mechanized tungsten-arc welding installation

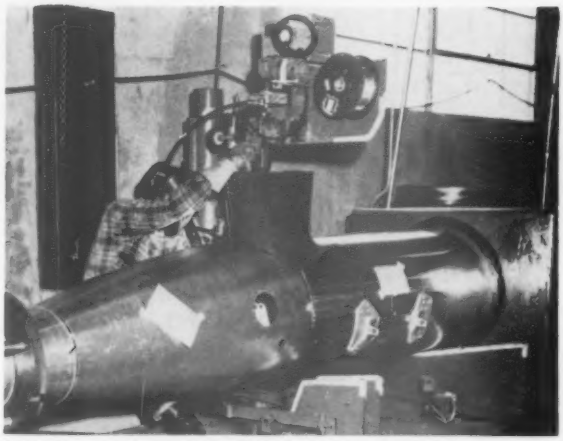

Fig. 5. A fuel tank begins to take final shape in this mechanized tungsten-arc welding operation. Here, the nose section and the center section are joined with a strong, clean, inert-gas-shielded weld

HUNDREDS OF DOLLARS SAVED EXTENDING SERVICE LIFE OF CUTTER HEAD

Here's a typical "on-the-job" application of build-up work that shows how profitable a gas-drive welder can be in keeping equipment operating and production moving.

Any hard surfacing is but a matter of depositing weld metal with properties that resist impact and abrasive wear on moving or working parts—extending the service life of the part many times the original.

This job happens to be on a sand sucker for a mining operation (see Fig. 1), but there are many applications of build-up on similar equipment that is subjected to hard abrasive use. The important thing of course is the cutting of "down time" to a minimum and to avoid costly tear downs and expensive replacement parts.

Here for example the cutting head on this sand sucker gets terrific wear as it digs in the sea sands of Florida for mining heavy minerals.

The cutter head itself weighs approximately three quarters of a ton and when in use, rotates at a speed of 30 rpm, working at a depth of 17 feet. Once a week, usually a Saturday morning, is set aside for rebuilding the cutter head. This is done by a regular member of the working staff who has had little previous welding experience. The rebuilding takes 4 to 5 hr, using 20 to 25 lb of electrodes.

The most wear is not on the blades that are made on 1-in. steel but rather on the teeth of the head since this is the part that actually does most of the digging. The teeth are of manganese steel, 3 in. wide, 8 in. long and $2^1/_2$ in. thick at the blade, tapering to a rather sharp edge. Here again, more wear occurs on the outside

Based on a story by Hobart Brothers Co., Troy, Ohio.

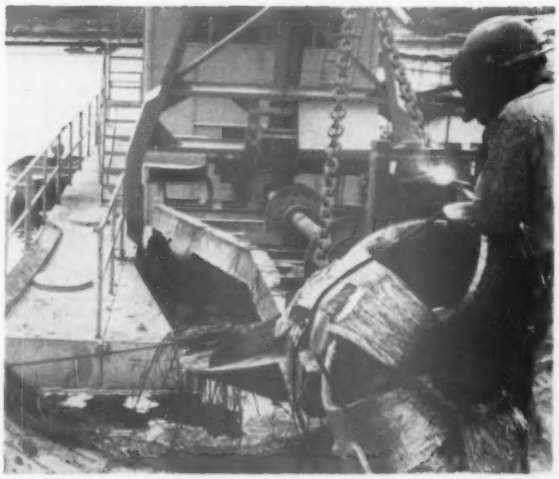

Fig. 1 Rebuilding in the field of cutting head on sand sucker used for mining heavy minerals

of the teeth, where friction is greatest. There are three teeth on each of the five blades that get the welding treatment each week.

When the cutter head becomes worn beyond advisable field repair, it is then replaced and taken to the shop for completely rebuilding, usually about once every 6 months. Actually, it takes less man-hours to rebuild it in the field every week than to replace the cutter head.

A 250-amp gas-drive welding machine is used for the job and $^5/_{32}$ or $^3/_{16}$-in. surfacing electrodes, welding at 130 to 200 amp depending on the electrode size and cable length.

Society News

Welding Show to Be Held in Major Metropolitan Center for First Time

The Welding Show, the annual exposition sponsored by the AMERICAN WELDING SOCIETY, makes its first entrance into a major metropolitan center this year.

In the heart of the rich Delaware River Valley, at Convention Hall in Philadelphia, the show will take place April 9th to 11th, inclusive.

Coincidentally with the move into one of the biggest industrial areas in the country, the days of the show have been rearranged to provide maximum convenience for those attending the annual Spring Meeting. This meeting runs from Monday, April 8th, through Friday, April 12th. This year, the show, instead of running from Wednesday through Friday, will be open from Tuesday through Thursday. Visitors will thus be able to schedule their time to take maximum advantage of both the technical sessions and the exhibits.

Previously, the show and Spring Meeting have been held in relatively smaller cities—Houston, Buffalo and Kansas City. However, the show has grown so rapidly, and the interest in welding is mounting so greatly, that it was decided to take both events into an area where there will be maximum benefit.

"The SOCIETY believes that the art and science of welding have made such significant strides in recent years that an exposition in a large industrial center is important both to the welding industry itself as well as the entire metals field," National Secretary Joseph G. Magrath has pointed out in a recent statement. "Welding, which once was regarded as a minor industrial art used principally for maintenance functions, today is a major industrial tool. Nowadays uses for welding knowledge are found wherever metals are processed and we believe that the exposition serves an important function in spreading that knowledge."

A feature inaugurated last year—that of having another engineering group sponsor some of the technical papers sessions—will be repeated in Philadelphia. The Committee on Electric Welding of the American Institute of Electrical Engineers will sponsor two sessions on resistance welding and one on arc welding, for a total of nine papers.

Other highlights of the Spring Meeting will include delivery of the Adams Lecture by Dr. DeWitt C. Smith, chief metallurgist, Electrode Division, Harnischfeger Corp., Milwaukee, Wis., on "Evolution of High-Tensile Weld Metal with Low-Hydrogen Electrodes"; an address by AWS President John J. Chyle; two sessions on nuclear reactors and irradiation effects, and a session on titanium and zirconium, among many others.

One of the major attractions will be the setting for the technical sessions. The new Sheraton Hotel, the most modern in the country, which will open its doors only a few weeks before the AWS meeting, will serve as the headquarters hotel. All social functions, including the annual President's Reception, will take place there.

Attendance is expected to soar over the 10,000 mark for the first time in the show's history. Increased attendance is anticipated not only because of the size of the Philadelphia industrial area, but also because the substantially increased number of exhibitors will give much broader scope to the displays. Virtually all exhibits will demonstrate products under simulated factory conditions.

Going to the 1957 AWS Spring Meeting and Welding Show?

Visitors at the Fifth Welding Show and Spring Meeting of AWS are urged to make their hotel reservations early. Demand for rooms in the Sheraton Hotel is expected to be extremely heavy, but other hotel space will be at a premium also.

Early reservations will receive priority.

Model of the new Sheraton Hotel in Philadelphia which will serve as headquarters for the 1957 National Spring Meeting of the American Welding Society, on April 8th to 12th

E.A.C. Educational Courses Committee Is Organized and Activated

The recently organized Subcommittee A—Educational Courses Committee of National Educational Activities Committee—is now busily engaged in the pursuance of its objectives, according to interim reports released by Subcommittee Chairman W. G. Leaman and Coordinator G. G. Luther.

The function of Subcommittee A is to prepare educational courses for Section and National use. It is the duty of this committee to write, revise, review or outline texts and programs in order to promote the Section and National educational courses on welding.

The members of this committee are: W. G. Leaman, General Electric Co., Buffalo, N. Y., *Chairman;* G. G. Luther, Air Reduction Co., Washington, D. C., *Coordinator;* W. R. Apblett, Foster Wheeler Corp., Carteret, N. J.; A. L. Tarr, U. S. Army, Fort Belvoir, Va.; H. D. Landis, Jr., Armco International Corp., New York; C. W. Monk, Alco Products Inc., Buffalo, N. Y.; L. Gilbert, Industry and Welding, Cleveland, Ohio; W. D. Doty, U. S. Steel Corp., Pittsburgh, Pa.; H. L. Ingram, Jr., Air Reduction Sales Co., New York; B. Gates, Budd Co., Philadelphia, Pa.; D. B. Howard, A. C. F. Industries, Berwick, Pa., and R. E. Somers, Bethlehem Steel Co., Bethlehem, Pa. Supplemental additions to this committee are anticipated from governmental agencies.

At the first meeting of this committee held in New York on Oct. 18, 1956, nine major courses were suggested and assignments made in accordance with the program. These courses to be designed for Section presentation and those individuals assigned the responsibility for the draft of outlines are as follows:

I. Welding Metallurgy, W. G. Leaman.
II. Welding Processes:
  (*A*) Resistance, H. D. Landis, Jr.
  (*B*) Oxyacetylene, H. D. Landis, Jr.
  (*C*) Brazing, H. D. Landis, Jr.
  (*D*) Inert Gas, W. D. Doty.
  (*E*) Submerged Arc, W. D. Doty.
  (*F*) Metallic Arc, W. D. Doty.
III. Welding Design, W. G. Leaman.
IV. Practical Weldability of Metals, A. L. Tarr.
V. Inspection for Welding, C. W. Monk.
VI. Automatic and Semiautomatic Welding, W. G. Leaman.
VII. Power Sources and Auxiliary Controls for Welding, W. R. Apblett.
VIII. Management and Economics for Welding, L. Gilbert.
IX. Thermal Cutting of Metals, H. L. Ingram, Jr.

A few of these outlines have already been prepared and, as expected, most of the above outlines were presented for discussion and further action by the committee at the next meeting of this body scheduled in Washington on Jan. 17, 1957.

The committee is presently making arrangements for the presentation of a two-session educational lecture series to be held at the forthcoming National AWS Convention in Philadelphia in April. It is being planned that representatives of the U. S. Navy Department will present this series embracing the topic of "Weldability of Ship Materials" with emphasis on notch ductility, low temperature performance and practical aspects of these contemporary research investigations as they relate to ship performance. Further announcement of this program will be made in a later issue of the "Welding Journal."

This subcommittee, acting under the direction of the National EAC, recently circulated a letter to the chairmen of all local Sections. This letter described the aims, purposes and functions of the EAC and indicated the organizational structure. The letter also solicited the names of individuals to be appointed by the Sections to be designated as liaison representatives between the National Educational Activities Committee and their local Sections. At this writing only about twenty-six Sections have been heard from. The chairmen of all Sections who have not as yet responded are again urged to submit the names of their liaison representatives to Clarence E. Jackson, Chairman of EAC, as soon as possible so that efforts of the National Committee and active subcommittees may be properly coordinated with local Sections for the enhancement of welding.

It is visualized that the Section—EAC liaison representatives will be the intermediaries in this over-all educational program at the local or Section level. Their efforts will be invited to assist the EAC and subcommittees whenever information and advice are needed by these committees in the pursuit of their appointed functions.

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Resolution

Fred T. Llewellyn

Old-time members of the AMERICAN WELDING SOCIETY received, in sorrow, the news of the passing on Feb. 19, 1956, of Fred T. Llewellyn, past-president of the SOCIETY and a bulwark of strength in the early days of the SOCIETY when such strength was needed.

Born in Cardiff, Wales, in 1869, he came to this country late in 1890 and specialized in steel construction. His company was absorbed by the American Bridge Co. and in one capacity or another, his entire professional career has been tied in with the United States Steel Corp. and its subsidiaries. Development of new products, particularly in the structural field, was his specialty so that he was readily attracted to welding and its potentialities in the structural field. Combining the qualities of a clear thinker, congenial personality and political acumen, he soon became identified with the executive side of the AMERICAN WELDING SOCIETY, becoming its President in 1928 and serving for a two-year term.

Mr. Llewellyn was instrumental in founding the Structural Steel Welding Committee of the SOCIETY and assisted in the founding of the first research program in this field and in the development of the first Building Code of the SOCIETY. He helped found the Welding Research Council in 1935 and served on the 1932 Handbook Committee of the SOCIETY.

He received the Samuel Wylie Memorial Medal in 1943. He retired from active duty in June 1939 and moved to Baton Rouge, La.

Characteristic of his own philosophy, Mr. Llewellyn caused the following quotation from Francis Bacon to be placed on the first report of the Structural Steel Welding Committee: "If a man will begin with certainties, he shall end in doubts; but if he will be content to begin in doubts, he may end in certainties."

RESOLVED: that this expression of sorrow in the loss of our associate, beloved by those who knew him and recognized by all as an outstanding engineer and humanitarian, be entered in the proceedings of the AMERICAN WELDING SOCIETY, and that a copy be sent to the immediate family of Fred T. Llewellyn with an expression of our most sincere sympathy in their loss.

Institute Student Membership Award

The Boyd Memorial Student Membership award has been instituted by the Northwestern Pennsylvania Section in honor of Wilfred R. Boyd who passed away in February 1956. It was felt that a student membership awarded to a young, deserving high-school student, who might show an interest in and further pursue a study of welding, would serve as a fitting tribute to Mr. Boyd.

In setting up the award, the organizing committee's first step was to contact the office of the Vocational Guidance Supervisor of the Erie Public Schools. They were very cooperative and wrote letters to the heads of the Science Departments in each of the local five high schools requesting them to submit a name of a student best fitting the qualifications. These qualifications were as follows: (1) junior year in high school; (2) genuine interest in science, with a preference toward metallurgy; (3) college preparatory course of study with intentions for college attendance; (4) scholastic record of a level indicative of ability to complete a course of higher education; (5) sufficient interest in welding to accept the student membership and attend chapter meetings.

A name was received from each school, the prospects were contacted and appointments were made for interviews. The interviews, in which questions were asked to decide which of the applicants rated highest in accordance with the desired qualifications, were conducted by three members of the committee. Frank Chimenti of Erie, Pa., was selected as the winner.

Prior to the first fall meeting, Mr. Chimenti was taken on two half-day plant tours with special emphasis on welding, and a day's session in a job welding shop with an opportunity to see and do simple welding and oxygen cutting. At the first fall Section meeting, the AMERICAN WELDING SOCIETY Student Membership was given to him and an invitation to attend the season's meetings as a guest of the Section was extended.

The members of the Section believe that the Boyd Memorial Award is serving the purpose intended, and it is their desire to continue its existence as long as support and interest is shown by local public school personnel and applicants.

Plans Completed for First Central States Welding Conference

The Cincinnati Section of the AWS has completed plans for its Annual Central States Welding Conference, scheduled to be held on Feb. 28 and Mar. 1, 1957.

The sponsors propose to reach approximately 1000 AWS members in Ohio, Indiana, Kentucky and West Virginia as well as 200 leading industries in these areas with their literature. Their preliminary polling of members in the above areas indicated a strong interest from which they feel that a minimum of 400 members will attend the Conference. Headquarters for the Conference will be housed in the Sheraton Gibson Hotel located at 5th and Walnut St., Cincinnati, Ohio. This establishment, Cincinnati's finest convention headquarters has made arrangements for approximately 25 exhibit booths, auditorium, dining facilities and room accommodations.

The complete program for the Conference is given on page 158 of this issue of the JOURNAL.

10th Western Metal Congress to Be Held on March 25th to 29th

The 10th Western Metal Exposition, March 25th to 29th in Los Angeles, will fill Pan-Pacific Auditorium and three huge canvas pavilions, it has been announced by William H. Eisenman, national secretary, American Society for Metals.

This will be the first time the ASM show has occupied a third pavilion. Last time in Los Angeles it needed only the auditorium and two canvas annexes.

The 10th Western Metal Congress, on the same dates in Los Angeles' Ambassador Hotel, will include technical sessions by the Los Angeles Section of AMERICAN WELDING SOCIETY, American Society for Metals, Society for Non-Destructive Testing and American Institute of Mining and Metallurgical Engineers.

A titanium conference, like the first one given at university level two years ago in cooperation with New York

University but of greater scope, also will be sponsored by ASM's Metals Engineering Institute during the congress.

Enroll as Supporting Company Members

Supporting Company Member enrollment applications have been received from the following organizations:

Freeport Sulphur Co., Port Sulphur, La.

Charles F. Guyon, Inc., Fabricating Division, 1000 S. Fourth St., Harrison, N. J.

Lyons Iron Works, Inc., 62 Maple St., Manchester, N. H.

These enrollments became effective on Jan. 1, 1957.

In addition to the above, the International Steel Co., 1321 Edgar St., Evansville 7, Ind., became a Supporting Company Member on Nov. 1, 1956. This company's name was erroneously reported as International Nickel Co. on page 1242 of the December 1956 issue of THE WELDING JOURNAL.

Hardeman Becomes Sustaining Member

Paul Hardeman, Inc., 10955 W. 6 Pico Blvd., Los Angeles 64, Calif., has recently become a Sustaining Member of the AMERICAN WELDING SOCIETY. The activities of this organization cover engineering and construction work in the fields of rocket and missile test stands, liquid oxygen facilities, oil and gas pipe lines, petroleum refineries, chemical and petro-chemical plant. Also included are research, development and manufacturing of welded products at fabrication plants in Los Angeles, Bakersfield and Denver. Engineering offices are located in Los Angeles, San Diego, Bakersfield, Sacramento, Denver, Jacksonville, Fla., Linden, N. J., Caracas, Venezuela. Charles W. Johnston has been appointed Sustaining Member representative.

Sponsor Educational Course on Resistance Welding

A series of lectures on resistance welding were held at the University of Buffalo under the sponsorship of the Niagara Frontier Section on Nov. 13, 20, 27 and Dec. 4, 1956. The principal purpose was to fully acquaint users with the new RESISTANCE WELDING HANDBOOK of the AMERICAN WELDING SOCIETY, and to present new developments in the industry as well as to assist those unfamiliar with resistance welding to understand its possibilities as a means of cost reduction.

Registrations for this course were solicited by personal contact and by mailings, resulting in an enrollment of 138, of which 30 were AWS members and 108 nonmembers. There was an average attendance of 109 per night for the lecture series and 130 attended the closing plant tour through two of Harrison Radiator's plant.

The details of the course were as follows:

LECTURE 1. (*a*) General introduction. (*b*) Explanation and discussion of first half of AWS Resistance Welding Test. *Speaker:* Emmett Craig, Federal Machine & Welder Co.

LECTURE 2. Explanation and discussion of second half of AWS Resistance Welding Text. *Speaker:* Foster Woodward, Taylor Winfield Corp.

LECTURE 3. (*a*) Explanation of various types of controls and control circuits. *Speaker:* Stuart Rockafellow, Robitron Corp. (*b*) Demonstration of methods and controls with equipment. *Speaker:* William Smith, Weltronic Co.

LECTURE 4. Quality control, preparation of parts, maintenance. *Speaker:* George W. Zeis, Harrison Radiator.

SESSION 5. Proposed tour through Harrison Radiator to see actual jobs in operation.

Because of the enthusiasm shown by the attendees, the Niagara Frontier Section proposes to hold a similar series during the Fall of 1957 covering resistance seam welding, flash welding and alloy welding.

Resolution

Harry Clow Boardman

At the very pinnacle of a notable engineering career, Harry Clow Boardman was stricken with a fatal malady. After a heroic two-year struggle, the end came on Aug. 6, 1956, thus writing finis to a lifetime of accomplishment as an author, editor, engineer, teacher and humanitarian. Dr. Boardman was born in Plainsfield, Ill., on Apr. 29, 1887. He was graduated from the University of Illinois in 1910. He received his Civil Engineering Degree from the University of Illinois and held an honorary Doctor of Engineering Degree from the South Dakota State School of Mines. He served the AMERICAN WELDING SOCIETY over many years in various capacities including the Chairmanship of the 1950 Welding Handbook Committee, member of the Board of Directors and its President from 1938 to 1939. In recognition for some of his services, he received the Miller Medal in 1941 and was made an honorary member of the SOCIETY in 1945. He was a long-time member of the Welding Research Council and its Chairman for a three-year term 1949–52. He was connected with the Pressure Vessel Research Committee from its inception and served for a short period as its Chairman. In 1954 he became Chairman of the Boiler and Pressure Vessel Code Committee of the American Society of Mechanical Engineers. He was active in at least six other engineering societies and at the time of his death, was Director of Research of the Chicago Bridge & Iron Co.

Typical of his perseverance and sense of professional obligation, he continued to serve the Boiler and Pressure Vessel Code Committee and the Welding Research Council up to within two weeks of his death.

His keen analytical mind, his warm friendship, his sympathetic understanding, his patience with human frailties and his judicious temperament made him a leader and endeared him to all with whom he came in contact.

RESOLVED, That this expression of sorrow for the passing of Dr. Harry Clow Boardman, and recognition of his achievement and humane qualities be entered in the records of the AMERICAN WELDING SOCIETY, and that a copy be sent to Mrs. Boardman with an expression of our most sincere sympathy in her loss.

Mark Your Calendar
Now for
April 8–12, 1957
FIFTH AWS WELDING SHOW
and
National Spring Meeting and
38th Annual Meeting
Philadelphia, Pa.

CENTRAL STATES WELDING CONFERENCE

HOTEL SHERATON GIBSON

Cincinnati, Ohio

Feb. 28–Mar. 1, 1957

Sponsored by AWS Cincinnati Section

THURSDAY, FEBRUARY 28th

9:00 A.M. Registration
10:15 A.M. Welcoming Remarks
C. T. Whalen, Chairman, Cincinnati Section, AWS, Ohio Valley Sales Co.

MORNING SESSION

10:30 Introduction to Welding
J. J. Chyle, President of the AMERICAN WELDING SOCIETY
11:15 Fundamental Physics and Metallurgy of Welding
R. B. McCauley, Professor and Chairman, Department of Welding Engineering, Ohio State University

12:30 P.M. LUNCHEON

AFTERNOON SESSION

2:00 Weldability and Evaluation
Dr. R. C. McMasters, Professor, Department of Welding Engineering, Ohio State University
3:30 Joining Processes
D. C. Martin, Consultant, Metals Joining Division, Battelle Memorial Institute

5:00 SOCIAL HOUR

EVENING, SESSION

6:30 DINNER
Toastmaster: H. E. Schultz, Manager, Product Applications and Processing, Flight Propulsion Laboratory Dept., Air Gas Turbine Division, General Electric Co.
8:00 Welding and Education
R. B. McCauley, Professor and Chairman, Department of Welding Engineering, Ohio State University

FRIDAY, MARCH 1st

MORNING SESSION

9:30 Engineering Requirements
W. L. Green, Instructor, Department of Welding Engineering, Ohio State University
10:30 Manufacturing Requirements
A. N. Kugler, Welding Engineer, Air Reduction Sales Co.
11:30 Design and Application
Van Rensselar P. Saxe, Consulting Engineer, Baltimore, Md.

12:30 P.M. LUNCHEON

AFTERNOON SESSION

2:00 Ultrasonic Welding
E. E. Wiesmantle, Chief Materials and Processing Engineering, Aeroprojects Inc.

3:00 Magnetic Force Welding
E. J. Funk, Experimental and Development Engineer, Precision Welder and Machine Co.
4:00 Panel of Experts

Exhibit booths will be open afternoons and evenings of both days

Many "Big" Fires Caused by Welding in Industrial Plants

Gas and electric welding and cutting are two of industry's most useful and indispensable tools, but the careless use of this equipment continues to show an adverse fire record. For example, in 1955, of the 225 building fires in the United States and Canada where the individual fire loss exceeded $250,000, welding and cutting operations were responsible for as many fires as any other single cause.

Though welding is an important fire cause, cutting is the big offender because sparks and molten metal from cutting torches travel farther and are more likely to cause fires than sparks from gas or electric arc welding.

Portable equipment causes the greatest number of fires. Outside workmen frequently are not familiar with plant operations or the hazards that may exist, and as a result may not take the necessary precautions.

However, welding and cutting fires are preventable through proper planning. Some person in authority, usually the chief fire protection or safety engineer, should be responsible for the safety of all such operations whether performed by his own company's welders or by outside contractors. A permit should be obtained by the welders or cutters from the responsible individual before they start their work. This responsible person should check the location for fire hazards and consult the nearest person in authority at the welding or cutting site for his knowledge of serious fire hazards that may be present and specify the precautions that must be taken before he issues a signed permit.

When possible, cutting or welding work should be moved to a safe location. A noncombustible room containing no appreciable amount of combustibles, or a sprinklered area containing no flammable gases or liquids or other fast burning materials, is considered a safe location. It is known that, of all the fires caused by welding and cutting, about 80% are caused by cutting operations in some place other than a permanent safeguarded location.

The applicable standards for good gas welding and cutting practices are the NBFU Standards for Gas Systems for Welding and Cutting No. 51, and, for electric welding and cutting, the American Standards for Safety in Electric and Gas Welding and Cutting Operations, ASA Z49.1. A copy of the former may be obtained by writing to the Engineering Department, National Board of Fire Underwriters, 85 John St., New York 38, N. Y. For the latter, write to the American Standards Assn., 70 E. 45th St., New York 17, N. Y.

●

EDITOR'S NOTE: The above has been reprinted from a Bulletin of the National Board of Fire Underwriters. Copies of Standard ASA Z49.1 may also be obtained by writing to the AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

When a low-hydrogen electrode . . .

Has an iron-powder coating . . .

Yet operates in all positions . . . .

WHY

use anything but Jetweld LH-70 for hard-to-weld steels

JETWELD LH-70 by LINCOLN

Low-hydrogen, iron-powder, E-6016 electrode for high-speed welding of hard-to-weld steels

DEPENDABLE WELDING FOR
Crack sensitive steels
Alloy steels without preheating
Medium to high carbon steels
Free machining steels

HIGHER DEPOSITION RATE
than conventional low-hydrogen electrodes

EASY OPERATION
in all positions
with high currents

CONFORMS TO
class requirements of E-6015, E-7015 and E-7016.
Meets X-ray requirements.

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Lincoln . . . one dependable source for all your arc welding needs

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Section News and Events

as reported to Catherine O'Leary

ELECTRODES

Birmingham, Ala.—A joint meeting of the *Birmingham* Section with the Birmingham Chapter of the American Society for Metals was held on December 4th at Gulas' Restaurant. An extremely interesting talk was given by E. H. Turnock AWS, director, metallurgical and chemical research, Westinghouse Electrode Division, Montevallo, Ala. Mr. Turnock spoke of the trends toward faster electrodes for cutting production costs. He showed some excellent high-speed color films of the welding arc and puddle.

A social hour and dinner preceded the technical meeting.

RESISTANCE WELDING

Cleveland, Ohio.—On Dec. 12, 1956, 146 members and guests of the *Cleveland* Section enjoyed dinner and a talk given by J. F. Cantalin AWS, engineer-in-charge of the Electrical and Hydraulic Department of Production Activity, Fisher Body Division, G.M.C. Mr. Cantalin's subject was "Resistance Welding and Its Future." A panel of welding authorities were also present to answer questions arising on the subject. The panel consisted of: J. W. Deffenbaugh, Federal Machine and Welder Co.; J. W. Cooper, Taylor-Winfield Co.; C. R. Schroeder, Weldaloy Products Co.; Wm. Farrell, Sciaky Bros.; S. Hinds, Robotron Co.; A. W. Black, Republic Steel & Tube Co.; J. Lott, North American Aviation; J. Murphy, Ford-Bedford Stamping Plant.

Among other things, Mr. Cantalin pointed out that resistance welding is advantageous for cost reduction in the automotive industry. Automation and mass production play an important part toward this end. There are many cases where basic resistance welders have been redesigned to fit particular needs. There are many problems incurred in welding of car bodies. The spot welding equipment is often designed to fit the job. In many cases jigs and fixtures must be built around the spot-welding machine.

Approximately one hour was spent after the talk with questions directed to the panel of experts.

LADIES' NIGHT

Denver, Colo.—A total of 63 members and guests attended the annual Ladies' Night dinner meeting of the *Colorado* Section held on December 11th in the Festival Room of the Oxford Hotel.

A very entertaining talk was given by Dr. Joe W. Lewis, chief of the Isotope Laboratory of the St. Francis Hospital in Colorado Springs, Colo. He described, in nontechnical terms, some of the many uses of radioactive materials. His delivery was in a humorous manner and was very well received.

CONTACT RESISTANCE

Columbus, Ohio.—Twenty-one members of the *Columbus* Section met at the Riverside Rathskeller on December 14th to hear Dean William B. Kouwenhoven AWS, Johns Hopkins University. The Dean's subject was "Contact Resistance and Its Control" and he described the experiments he

RESISTANCE WELDING DISCUSSED IN CLEVELAND

John F. Cantalin (*center*) was principal speaker at the Dec. 12, 1956, meeting of the Cleveland Section. He dealt with resistance welding and its future. Guest panelists, some of whom are shown above, also spoke on the same subject. Left to right are J. Murphy, R. C. McMaster, Mr. Cantalin, J. F. Deffenbaugh and C. R. Schroeder

Other members of the panel are shown in the above group. (*Left to right*) J. H. Cooper, S. Hinds, J. Lott, A. W. Black and Wm. Farrell

"Problem weld between dissimilar alloys ...Inco-Rod "A" takes it in stride!"

says Rolock's Chief Engineer

Rolock Inc., Fairfield, Conn., builds equipment for high-temperature service. To them, difficult welds between dissimilar alloys are an everyday affair.

The weldor above, for example, is completing fabrication of a furnace retort. The flange is steel. The corrugated section at right is Inconel* nickel-chromium alloy . . . to withstand temperatures up to 2050° F.

Trouble at the joint? None at all, now that Rolock is using Inco-Rod "A"* electrode!

"With previous electrodes," writes George Wardwell, Chief Engineer at Rolock, "considerable reworking was needed to eliminate cracks and fissures in problem welds like this one. Now, with Inco-Rod "A" electrode, our weldors work with assurance that the finished weld will be sound in every respect."

Get sound joints when *you* weld dissimilar alloys. With Inco-Rod "A" electrode, you get strong, ductile welds . . . corrosion-resisting . . . of X-ray quality.

And versatile Inco-Rod "A" electrode produces these quality welds in *many* alloy combinations: among ferritic and austenitic stainless steels . . . low alloy and mild steels . . . high-nickel, and other alloys.

What's more, Inco-Rod "A" . . . the electrode with the green flux coating . . . is easy to use. Operates in all positions. Gives spray-type arc and easy slag removal. And manufacturers, who have found that they needed to preheat to prevent hot cracking or fissuring in the weld metal when using other electrodes, find that with Inco-Rod "A" electrode they can produce strong, ductile welds ***without pre-heat.***

Call on Inco whenever you're faced with a "problem" weld between dissimilar alloys. If the problem's *now*, write our welding specialists today!

*Registered trademark

THE INTERNATIONAL NICKEL COMPANY, INC.
67 Wall Street New York 5, N. Y.

INCO-ROD "A" electrode is supplied in 14-inch lengths in four diameters, 3/32-, 1/8-, 5/32-, and 3/16-inch (the 3/32-inch is center grip) . . . packed in 5-lb. asphalt-lined containers.

INCO **Welding Products** • electrodes, wires, fluxes

COLUMBUS SPEAKER

Dean W. B. Kouwenhoven was the guest speaker at the December 14 meeting of the Columbus Section. His subject was Contact Resistance and Its Control

has conducted over the past several years in which he determined that the total contact resistance is the sum of "spreading resistance" and "interface resistance." Spreading resistance is the larger of the two components and may be lowered by increasing the number of microscopic contact points.

The meeting was enjoyed by all those present.

PLANT VISIT

Detroit, Mich.—On October 19th, the *Detroit* Section made a most profitable plant visit to a major resistance-welding machine manufacturer, the Link Welder Corp., where a very fine buffet dinner and refreshments were served. The popularity of the event was demonstrated by the fact that over 410 members and guests enjoyed the hospitality of Link Welder.

The attendance was from the entire automotive and allied industries. An added attraction was the D. Jerkiss Quartet who entertained the group with all the popular tunes to make the evening a success.

Visitation was arranged by George Bracken, general manager, and his associates. The new enginering section as well as the expansion of the plant facilities, were greatly admired by the group.

AUTOMATIC SURFACING

Houston, Tex.—With an attendance of 73, the *Houston* Section convened on November 26th for a dinner meeting held at the Ben Milam Hotel, where an interesting session on automatic hard surfacing as it applies to abrasive problems took place. R. Arnoldy [illegible] was the speaker. Mr.

SECTION MEETING CALENDAR

MARCH 1

NORTH CENTRAL OHIO Section. Lima, Ohio. Dinner meeting. "Resistance Welding of Heavy Structures," Wm. J. Farrell, Sciaky Bros. Inc.

PHILADELPHIA Section. Panel discussion. "Torch, Tungsten Arc, and Furnace Brazing, Where and Why?"

MARCH 4

LEHIGH VALLEY Section. Dinner 6:30 P.M. Meeting 8:00 P.M. Walp's Restaurant, Allentown, Pa. Joint meeting with ASCE.

MARCH 8

COLUMBUS Section. Columbus, Ohio. Fundamentals Course Graduation. "Special Applications of Resistance Welding." Columbus Section Panel.

ST. LOUIS Section. Forest Park Hotel. "Structural Welding," T. B. Jefferson, Editor, The Welding Engineer.

MARCH 12

DAYTON Section. Dayton, Ohio. Dinner meeting. "The Compressed-Air Arc-Cutting Process," Myron Stepath, Arcair Co.

NEW YORK Section. Victor's Restaurant, 1 East 35th St., New York, N. Y. "Distortion and Its Control in Arc Welding," J. R. Stett, R C. Mahon Co.

MARCH 13

CLEVELAND Section. Cleveland, Ohio. Hotel Manger. "Design for Welded Plate Fabrications," Fred Plummer, Hammond Iron Works.

MARCH 14

KANSAS CITY Section. Dinner meeting. Golden Ox (Livestock Exchange Bldg.) Kansas City, Mo. "Structural Welding," LaMotte Grover, Air Reduction Sales Co.

MADISON Section. Eagle Club, Madison, Wis. "Stud Welding and Applications," H. Bartley.

PUGET SOUND Section. Technical meeting on "Joining of Plastics with Heat."

SAGINAW VALLEY Section. 7:15 P.M. High Life Inn, Saginaw, Michigan. "Mechanical Handling," Hank Behrms.

SUSQUEHANNA VALLEY Section. Joint meeting with ASM. Irem Country Club, Dallas, Pa. "Brittle Failure of Large Welded Structures," W. S. Pellini, U. S. Naval Research Laboratory.

MARCH 15

MARYLAND Section. Engineers Club, Baltimore, Md. "Which Welding Process and Why?" Orville T. Barnett, Armour Research Foundation.

YORK-CENTRAL PENNSYLVANIA Section. 6:30 P.M. Annual Oyster Bake. Tremont Sea Food House, Cleveland and Cottage Place, York, Pa.

MARCH 18

PHILADELPHIA Section. Philadelphia, Pa. "Structural Welding Design," P. C. Kavanagh, Praeger-Kavanagh Engineers.

ROCHESTER Section. Liederkranz Club, Rochester, N. Y. Dinner 6:30 P.M. Meeting 8:00 P.M. "Evaluation of Iron-Powder Electrodes," D. B. Howard, ACF Industries.

MARCH 19

NORTHWESTERN PENNSYLVANIA Section. Erie, Pa. "Automatic Brazing Machinery," C. A. McFadden, Selas Corp.

OLEAN and BRADFORD Section. 7:00 P.M. Castle Restaurant, Olean N.Y. "Design for Welded Plate Fabrication," A. F. Fino, Hammond Iron Works, Warren, Pa.

MARCH 20

PITTSBURGH Section. 8:00 P.M. Mellon Institute of Industrial Research, Oakland, Pa. "Compressed-Air Arc Cutting and Gouging," Myron D. Stepath, Arcair Company; "Cutting Aluminum Alloys," Paul B. Dickerson, Alcoa Process Development Labs.

MARCH 21

MICHIANA Section. South Bend, Indiana. "The New Process of Severing Metal and Heliarc Welding of Pipe," H. M. Gregoire, Linde Air Products Co.

SHREVEPORT Section. Colonial Room, Captain Shreve Hotel, Shreveport, La. Dinner 7:00 P.M. Meeting 8:00 P.M. "Manufacturing, Fabricating and Application of Clad Steel," Louis K. Keay, Lukens Steel Co.

MARCH 22

INDIANA Section. Plant tour C.A.A. Weir Cook Airport, Indianapolis. Dinner at Airport Restaurant.

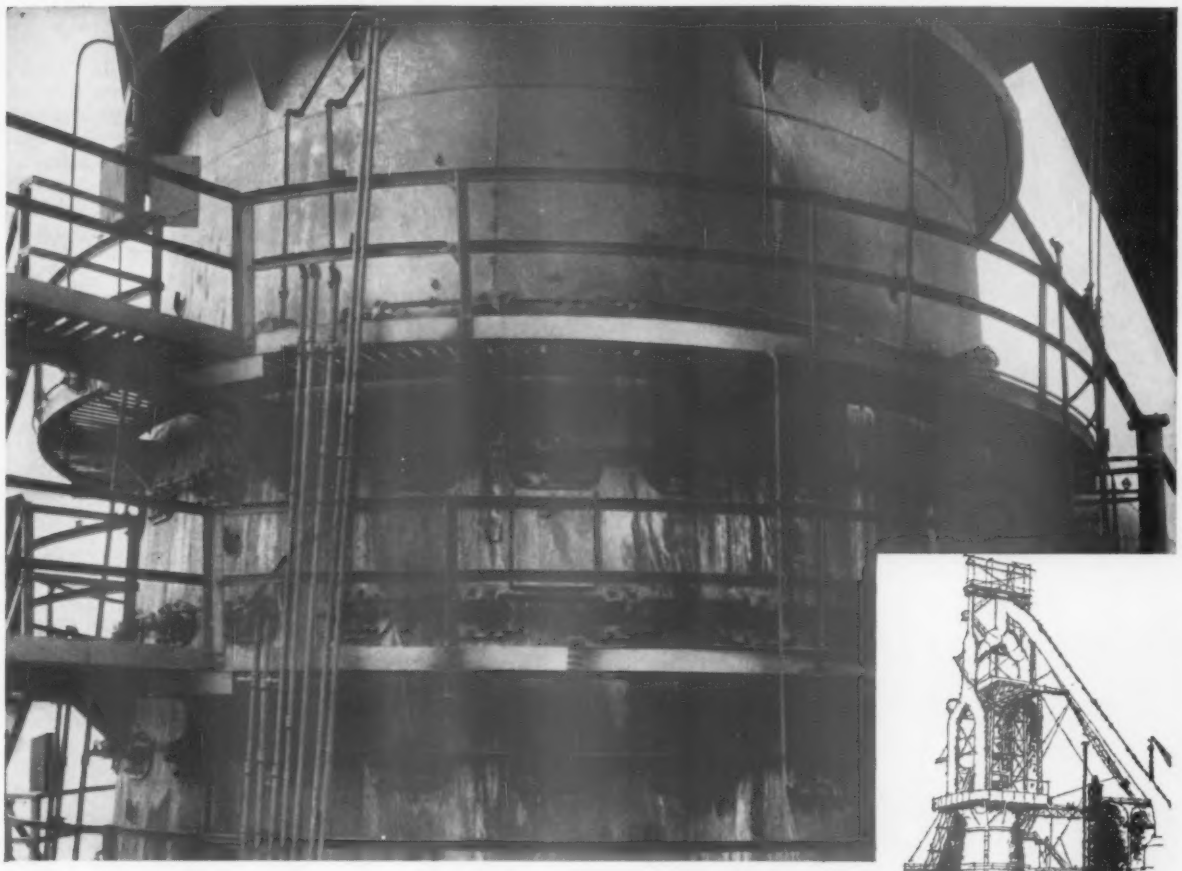
MILWAUKEE Section. Dinner 6:30 P.M. Meeting 8:00 P.M. Ambassador Hotel, Milwaukee, Wis. "Inert-Gas Mixtures for Tungsten-Arc Welding," T. McElrath, Linde Air Products Co.

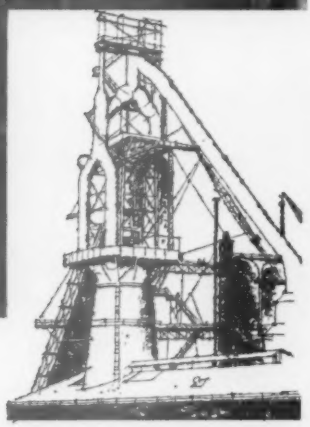
CHICAGO Section. Vogel's Restaurant, Hammond, Indiana. Dinner 6:30 P.M. "Stud Welding as It Is Today," R. Singleton, Nelson Stud Welding.

MARCH 28

NIAGARA FRONTIER Section. LaSalle Yacht Club, Niagara Falls, N. Y. Dinner 6:30 P.M. Meeting 8:15 P.M. "Control and Correction of Welding Distortion," LaMotte Grover, Air Reduction Sales Co.

Editor's Note: Notices for May 1957 meetings must reach Journal office prior to March 1, so that they may be published in April Calendar. Please give full information concerning time, place, topic and speaker for each meeting.





Why Airco 78E electrodes were selected to weld blast furnace "R"

A large eastern steel producer planned to build the giant blast furnace designated "R". Welding – with its obvious advantages of speed and economy – was naturally selected as the best method of construction.

But – which electrode would best do the job? These were the factors involved in making a decision:

(1) Quality of weld deposit. Welds had to be leak-proof to contain the 15 lb. psi pressure in the vessel. **(2)** High ductility and tensile strength were of prime consideration. **(3)** An all-position electrode was absolutely essential.

The Airco 78E electrode answered all these qualifications. Therefore, it was the logical choice. A total of 13,500 pounds of Airco 78E electrodes were used to weld the structure.

In building a blast furnace – or rebuilding an engine housing – Airco's Electrode Pocket Guide can help you take the guess work out of selecting the right electrode for the job. This 44-page booklet contains neatly condensed descriptions, applications and procedures – plus charts and graphs – for using the complete Airco line of electrodes.

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DETROIT SECTION VISITS LOCAL PLANT

A large group from the Detroit Section attended the plant visit to Link Welder Corp. on October 19th

Shown in front of the D. Jerkiss Quartet are (*left to right*) Gordon Kirkwood, vice-chairman of the Detroit Section; Chief Engineer Art Kelsey, General Manager George Bracken, Shop Supt. George Thompson, Asst. to General Manager George Prowse of Link Welder Corp.; and W. H. Smith, Section chairman

Arnoldy has his own company, Texas Alloy Products Co., Houston, and had many first-hand examples to discuss. The subject was especially interesting to members and guests of this area because of the applications to the petroleum industry of this area.

INERT-GAS WELDING

Bethlehem, Pa.—J. W. Cunningham AWS, of the Air Reduction Sales Co., Summit, N. J., was the speaker at the December 3rd technical meeting of the *Lehigh Valley* Section held at Walp's Restaurant.

Coffee speaker at the dinner which preceded the meeting was T. D. Shannahan of the Bethlehem Steel Co., Sales Department. Mr. Shannahan's talk on "Hobby on Wheels" was accompanied with color slides of old style automobiles.

Mr. Cunningham's talk on "Inert-Gas Welding" was illustrated with slides showing different applications of the process. Charts were also shown of the results of using argon, helium and CO_2 gases. High-speed motion pictures showing the difference between welding with argon and carbon dioxide shields were shown. Mr. Cunningham pointed out that inert-gas welding can be used on materials ranging from paper thin to metal 2 in. thick. Inert-gas welding can be used in a number of applications by varying electrodes and gas shields.

HOUSTON SPEAKER

R. Arnoldy pictured while delivering his talk on automatic hard surfacing before the Houston Section on November 26th

CHRISTMAS PARTY

Huntington Park, Calif.—In place of its regular technical session for the month of December, the *Los Angeles* Section, together with the *Aircraft and Rocketry Panel* and the *Long Beach* Section, held the annual Christmas party. This event, always a huge success since its inception, was held on Thursday, December 6th at the Elks Lodge in Huntington Park with 252 members and guests present.

The program was arranged and procured by Francis McGinley, past chairman of the Section, and featured such acts as Miss Toni LaRue, The Carlsons, Merle Taber, the Spade Cooley Show and Denny and Cameron. Master of ceremonies was Joey Rardin. Prior to the show, and all during the cocktail hour and dinner, the members and their guests were entertained by Trudy Hartman, accordionist.

Special guests were First Vice-President C. P. Sander and Francis C. Pope.

STAINLESS STEELS

Baltimore, Md.—Two speakers were featured at the November meeting of the *Maryland* Section held at the Engineer's Club on November 16th. Both speakers are connected with the Crucible Steel Company of America. The first speaker, Ken Matticks, stainless contact metallurgist, pointed out that there are more than 50 types of stainless steels, all identified by their AISI numbers in three different groups. Group 1: straight chromium hardenable, magnetic; Group 2: straight chromium, nonhardenable, magnetic; Group 3: nickel-chromium, nonhardening, nonmagnetic.

The second speaker, Jack Goodford AWS, chief welding engineer, showed about 125 slides in color of welding and welding procedures on all gages of stainless steel with electrodes, inert gas and consumable automatic applications in overlay of stainless steel

ONE wide-range "OXWELD" W-45 BLOWPIPE handles EVERY welding and heating job

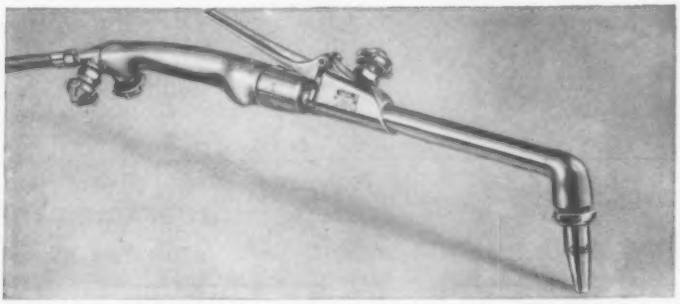
Trade-Mark

*NO OTHER SINGLE BLOWPIPE OFFERS THIS **EXTENSIVE RANGE!***

Anyone whose daily work includes welding and heating will readily appreciate the amazing wide range and versatility of the new OXWELD W-45 Blowpipe. Its 18 head sizes (2 to 300 cu. ft. per hr. capacity) provide a perfect flame for every metal thickness. Light sheet to heavy plate, *one blowpipe does it all!*

From chrome-plated tip to offset hose connections, the W-45 shows the results of over a decade of development work by LINDE engineers. Its exclusive "jiffy-lock" heads, "form-fit" handle, and advanced styling are as modern as guided missiles and atomic power. "O" ring gas seals, flame-stabilizing mixers of improved type, and many other innovations put this blowpipe far ahead of the field in economy, ease of operation, and low-cost maintenance.

See for yourself how you can enjoy tomorrow's operating standards today with an OXWELD W-45 Blowpipe. Ask your LINDE representative for a demonstration, or write for free booklet, F-8684.



CW-45 Cutting Attachment adapts the W-45 Blowpipe for cutting steel up to 8 inches thick.

Linde Air Products Company

A Division of Union Carbide and Carbon Corporation

30 East 42nd Street UCC New York 17, N. Y.

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In Canada: LINDE AIR PRODUCTS COMPANY
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Linde Trade-Mark

The terms "Linde" and "Oxweld" are registered trade-marks of Union Carbide and Carbon Corporation.

LOS ANGELES SECTION HOLDS CHRISTMAS PARTY

AWS members and guests enjoy cocktails at Christmas party prior to dinner and show

"Trudy," featured accordionist, entertains during cocktail hour

Dancers were one of featured acts at party

First Vice-President C. P. Sander (*extreme left*) shown seated at table for officers and special guests. F. Pope, J. Ross and G. Murphy are at right

Seated at Speaker's table are C. Smith, Section chairman, and F. McGinley, program chairman for party

on steel vats in the papermaking industry.

Coffee speaker was Dr. William B. Kouwenhoven AWS, dean emeritus of the School of Engineering, John Hopkins University, who spoke on the effects of electricity on the heart.

ELECTRODE COATINGS

South Bend, Ind.—The *Michiana* Section meeting held at Russ' Restaurant on December 13th was a wonderful example of the great effort put forth by speakers to keep their engagements and of the cooperation extended by many others when transportation breaks down. The scheduled speaker, H. F. Reid AWS, of the McKay Co., York, Pa., after a long day of frustrating airplane and airport riding, found himself an hour after meeting time 90 miles away and still looking for a break in the weather. The McKay area representative, Tom Collins AWS, with the Section's sincere approval, told Mr. Reid not to try to come any further as it seemed an impossible task to get to his destination that night. The Michiana Section wishes to express their appreciation to Mr. Reid and to all other speakers who work so hard to fulfill their promises even at times when the elements seem to go against them.

When it became apparent that Mr. Reid was not going to be able to reach South Bend, Mr. Collins was drafted to read the talk, since he had brought printed copies along. And so, though those present were disappointed not to have the meeting as scheduled, it was far from a loss—the gain in "esprit de corps" was appreciated by all.

MAINTENANCE WELDING

Milwaukee, Wis.—The December 14th meeting of the *Milwaukee* Section was held at the Ambassador Hotel.

T. B. Jefferson AWS, Editor of the *Welding Engineer*, gave an interesting talk on "Maintenance Welding," touching in general on the broad advantages of the use of welding as a tool for maintenance.

Mr. Jefferson outlined the progress that maintenance welding has made, giving some of his early experience as far back as 1933 on the Fort Peck Montana Dam, which at that time had the world's largest maintenance shop.

Mr. Jefferson concluded on the note that although welding is used largely in maintenance now, there is room for its increased use in the salvage of expensive machine parts and elimination of long time delays in obtaining replacements.

SURFACING APPLICATIONS

New York Mills, N. Y.—Technical speaker at the December 13th meeting of the *Mohawk Valley* Section was E. J. Lell AWS, vice-president in charge of sales for the Wall Colmonoy Corp., Detroit, Mich.

Mr. Lell presented a very interesting and enlightening talk on "Hard Surfacing and Its Applications." Many applications and problems were explained. Slides showing applications and charts showing physical properties of these applications and their reaction to extended temperature changes were shown. Mr. Lell was assisted at the slide projector by Arnold D. Arnaut AWS, district manager of the New York Sales Division of Wall Colmonoy.

SPEAKS ON T-1 STEELS

Paper on fabrication of T-1 steels was presented by Carl F. Ascenzi (*extreme right*) at the Dec. 18, 1956, meeting of the New Jersey Section. Shown with him are Frank P. Iapalucci (*left*), co-author of paper, and John Hila (*center*), Section chairman

FABRICATION OF T-1 STEELS

Newark, N. J.—A paper entitled "Fabrication of T-1 Steels" co-authored by Frank P. Iapalucci AWS, manager of Welding Engineering, and Carl F. Ascenzi AWS, chief welding engineer of the Baldwin-Lima-Hamilton Corp., was presented by Mr. Ascenzi to 75 members and guests of the *New Jersey* Section on Dec. 18, 1956, at the Essex House Hotel in Newark.

Illustrating with slides, Mr. Ascenzi gave a very complete review of the methods used in the manufacturing of these quenched and tempered steels, describing the different procedures for rolling pressure vessel and structural T-1 steel. Mr. Ascenzi also reviewed the chemical and physical characteristics and the machining and weldability.

The talk was preceded by a film in color entitled "Sea-Going Sea Horse" showing how to catch the big ones off the Pacific Coast of Mexico.

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STAINLESS STEELS

New York, N. Y.—Two prominent speakers were invited to address the December meeting of the *New York* Section which was held on December 11th. As a result, a capacity audience was in attendance. The speakers were J. A. Goodford AWS, chief welding engineer, and Kenneth A. Matticks, metallurgist, both of the Crucible Steel Corp., Harrison, N. J.

Mr. Goodford spoke on stainless steel plate materials of the martensitic, ferritic and austenitic groups. A number of slides were shown and a dissertation given on improper joint designs, the effects of overheating stainless steel during grinding and polishing, how to control carbide precipitation during welding, and which electrodes should be used.

Mr. Matticks covered the manufacturing process and the characteristics of the family of stainless steel, encompassing the groups mentioned above; in addition, he covered the new 200 Series. Also, the speaker showed a great number of slides which included the application of stainless steel as cladding for vessels used in refineries.

After the lectures, a question-and-answer period was held which became so interesting that the meeting ran much in excess of the time allotted. This clearly indicated that it was a most successful and educational discussion.

The meeting was held at Victor's Restaurant, 1 E. 35th St. An excellent dinner was served prior to the lectures.

SECTION QUIZ

Findlay, Ohio.—Approximately 180 members and guests of the *North Central Ohio* Section were present on December 7th and enjoyed a delicious dinner at the Findlay Country Club.

The technical meeting was a "Section Quiz." The teams participating were from Cleveland, Cincinnati, Toledo, Ohio and Stark Central Sections. Moderator was R. A. Ellison, program chairman.

Participants and judges were as follows:

Cleveland Section: L. K. Stringham, Lincoln Electric Co.; Frank Flocke, Thornton Co.; Bob Henry, Cleveland Diesel Division, General Motors; and Jack Mallet, Denton-Anderson Co. Toledo Section: Tom Ellis, Willy Motors, Inc.; Ed Brenner, Williams & Co.; M. Baughman, Air Reduction; J. Cammarata, Dana Corp.; and W. Schmidt, Toledo Edison Co. Cincinnati Section: G. Schauerte, Metal & Thermit Corp.; W. Klingeman, Precision Welder & Flexopress Corp.; H. Schultz, General Electric Co.; and R. Lossee, Lincoln Electric Co. Stark Central: J. Cooksey, Griscom Russel Co.; W. Heestand, Alliance Structural Co.; C. Brainard, Alliance Machine Co.; and J. De Stafano, Diebold, Inc. The judges were: W. R. Edwards, Marion Power Shovel, Marion, Ohio; F. E. Donathan, Superior Coach, Lima, Ohio; and O. C. Rowlinson, Ohio Locomotive Crane, Bucyrus, Ohio.

The Cleveland Section was the winner with 860 points. Stark Central was second with 810 points, and the Cincinnati Section was third with 715 points. The Toledo Section was fourth with 705 points. A plaque was presented to the winning team, with the compliments of the North Central Ohio Section.

MAKE PLANS FOR WESTERN METAL CONGRESS

Members of the AWS Los Angeles Section plan welding-papers programs for five days of 10th Western Metal Congress March 25th to 29th in the Ambassador Hotel, Los Angeles. Seated at table (*left to right*): David P. O'Connor, Section vice-chairman; Charles W. Concannon, chairman of the AWS program committee for the Metal Congress; John R. Ross, Section secretary-treasurer. Standing: C. B. Smith, Section chairman

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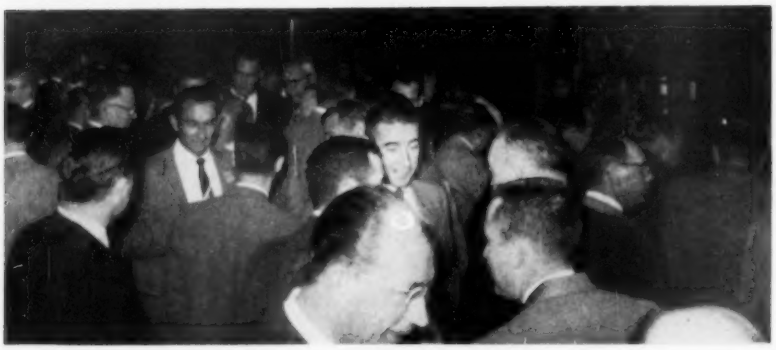

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At the McLendon Manufacturing Company . . .

Production increased 15% on engine mounts

"By switching to this General Electric welder we have all but eliminated downtime and have increased our production 15%. Our welding section is now 100% General Electric."—J. W. McLendon, President, McLendon Manufacturing Company.

"I'm sold on the machine. It has better arc striking quality than our previous m-g sets. Anyone can strike an arc with this welder. With the added arc force you get an instantaneous start of the weld."—W. E. McWilliams, weldor.

EXECUTIVE AND WELDOR AGREE IN SPECIFYING G-E WELDERS

Producing engine mounts that must withstand the vibration of 1500-horsepower, 3000-rpm engines, the McLendon Manufacturing Company demands welding equipment that gives them:

A STABLE ARC

STABILITY OF CURRENT

FLEXIBILITY OF CONTROL

ECONOMY OF OPERATION

With their General Electric 300-amp d-c rectifier welders they get all this . . . and more. J. W. McLendon, president of the Tulsa, Oklahoma firm, says, "After testing one of these units, I suggested we standardize on General Electric equipment. Now our welding section is 100% General Electric. By using the G-E welder and your W-616A and W-1016A electrodes we have a combination that gives cleaner, more uniform, and smoother welds."

Back in the plant, weldor W. E. McWilliams is pleased with Mr. McLendon's choice of equipment. "With our old welders," he says, "we had a lot of downtime for maintenance and repair. This always meant somebody was out of work for a while. Now, as Mr. McLendon says, with the General Electric equipment, we have reduced downtime practically to zero, and increased our production 15%."

The engine mounts welded by McLendon must be lightweight, yet strong enough to withstand the vibration from the powerful aircraft-type engines.

After the engine is placed on the mount, it is lifted onto a specially designed truck body. It is then used in oil producing areas as part of a mobile fracturing unit for increasing the flow of oil wells. To date there have been no structural failures on any of the great number of McLendon units in operation.

General Electric rectifier welders are designed to make the weldor's job easier while making surer strength welds at lower cost.

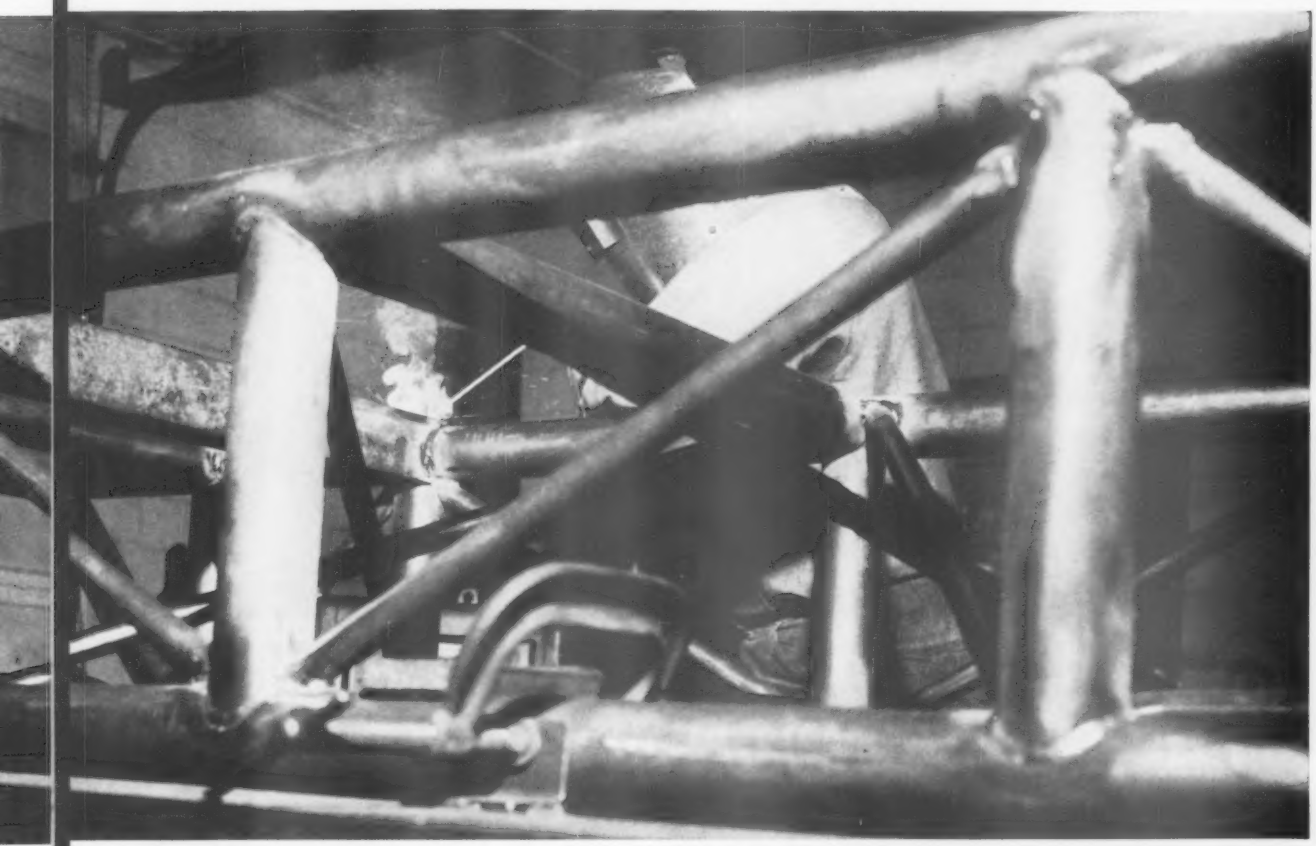
Exclusive, full-time arc force control permits welding with shorter arcs than are possible with any other rectifier welder.

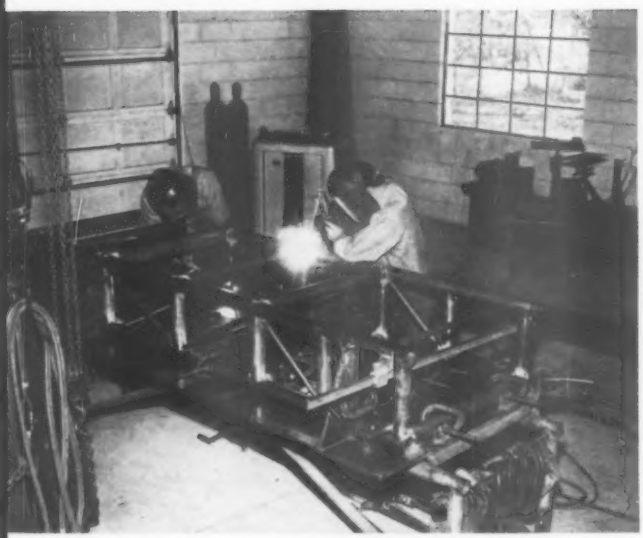
The large, easy-to-read current scale is expanded at the lower range. It allows for pinpoint settings at these lower ranges, where accuracy is most important.

For more information see your nearest General Electric Welding Distributor. He is listed in the yellow pages of the telephone book under "Welding Equipment, General Electric." Or write directly to Section 711-7, General Electric Company, Schenectady 5, New York.

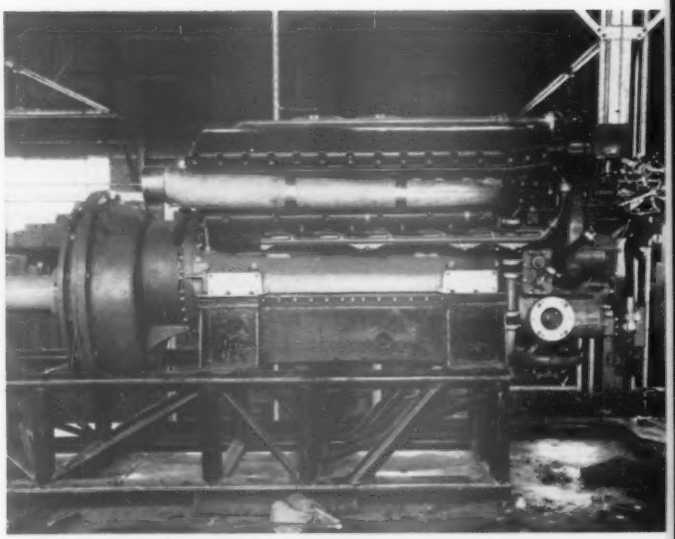
GENERAL ELECTRIC

with General Electric rectifier welders





Weldors Kurt Mueller and William McWilliams finishing off an engine mount. It consists of 85 separate pieces of tubing requiring 172 individual welds varying from one to 30 inches long. Tolerances are $\pm \frac{1}{64}''$ high and $\frac{1}{32}''$ wide. Metal used is 10-gage seamless tubing, AISI 1015 steel, .25 carbon.



A 1400-pound, 1500-horsepower engine on frame welded with G-E rectifier welders and electrodes. Engine and frame are placed in truck bodies and used with other equipment for work in increasing flow of oil wells. Welded frame must be able to withstand terrific vibration encountered in field operation.

For the tough job of fabricating the steel framework of the Recreation Bowling Center of Houston, Texas, Southern Steel Erectors uses General Electric engine-driven welders. Low maintenance features impress company owner, Mr. R. O. Matthews.

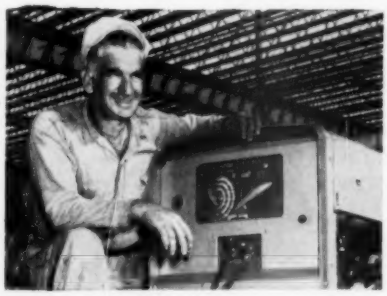
Contractor picks General Electric welders for his tough outdoor construction jobs

Southern Steel Erectors, a Houston, Texas contractor, demands welding equipment that is tough enough to withstand the punishment found in outdoor steel framework construction.

No wonder, then, that R. O. Matthews, owner of the firm, now specifies General Electric engine-driven welders for his welding jobs.

He finds that with General Electric equipment, repairs no longer cause cost-consuming downtime for his company. Some of the reasons for this are:

- **Extra-heavy-gage metal construction minimizes accidental damage.**
- **No exciter is required. A potential trouble spot is eliminated.**
- **Constant-pressure brush springs and pre-set brush holders require no adjustment—are factory set.**
- **Pre-lubricated bearings all but eliminate bearing maintenance.**
- **Steel driving disk holds generator exactly in line with engine shaft—has no expendable parts.**



Weldor E. D. Keck says that during his 15 years as a weldor, using nearly every make of equipment, he has found the new General Electric engine-driven welders are the easiest to use and produce the best welds.

Mr. Matthews also says, "This G-E engine-drive has more power than other welders, and we save on fuel, too. We use only one gallon of gas per hour. Also, I like its adaptability to a wide range of rod sizes."

Weldor E. D. Keck says, "I've found in my 15 years as a weldor that the new G-E engine drive gives you a better weld—easier, because there is no fluctuation. All you do is set the machine and it will remain true and constant."

General Electric's new line includes three sturdy ratings, 200, 300, and 400 amperes. For more information contact your nearest General Electric Welding Distributor, listed in the yellow pages of the telephone book. Or, write section 714-6, General Electric Company, Schenectady, New York.

Don't miss the General Electric display at the AWS Welding show April 9-11 at Convention Hall in Philadelphia. The display will be in booth No. 322.

GENERAL ELECTRIC

SPEAKS ON STAINLESS STEELS

Vincent T. Malcolm addressed the Northeast Tennessee Section on November 19th. He is shown above (*left*) with E. C. Miller (*center*) and J. C. Thompson

STAINLESS STEELS

Knoxville, Tenn.—On November 19th, the *Northeast Tennessee* Section had a joint dinner-meeting and technical session with the American Society for Metals. The meeting was held at the Waldorf Restaurant where a very enjoyable meal was served.

Guest speaker at the technical meeting was Vincent T. Malcolm AWS, director of research for the Chapman Valve Manufacturing Co., Indian Orchard, Mass.

Although Mr. Malcolm unfortunately had lost his slides and much of his prepared material while in transit to Oak Ridge, he gave an excellent talk on "Methods of Welding Austenitic Stainless Steels." Since Mr. Malcolm is a member of the ASTM Boiler and Pressure Vessel Committee, he was able to present a clear-cut picture of the basic requirements and specifications for acceptable welds according to code standards. In addition, his background and experience in metallurgy, metals research and welding techniques, enabled the listeners to obtain valuable information pertaining to recent developments in the metals field.

EDUCATIONAL PROGRAM

Phoenix, Ariz.—The *Phoenix* Section has inaugurated a plan designed to encourage student interest in the Section meetings and other activities. The first meeting was held on November 20th and the results were gratifying. Several companies were invited to sponsor one student for a meeting per year. Trade schools and welding instructors were invited to select an outstanding student from welding schools and classes, and bring him to the monthly meeting as a guest of the Section.

FABRICATION OF BEAMS

Seattle, Wash.—The November meeting of the *Puget Sound* Section was held in the Frye Hotel. The guest speaker was D. B. Peterson, regional engineer of Linde Air Products Co., San Francisco. His subject "Fabrication of Beams for Bridges and Freeways" was illustrated by numerous colored slides. It was apparent that Mr. Peterson was very well versed in his subject which ranged from qualification, through fabrication, to final completion of many bridges and, more particularly, the San Francisco Freeway. Judging from the number of questions asked from the floor by those present, it was obvious that a great interest had been shown on this timely subject: so much, in fact that the speaker only had enough time to catch his plane for San Francisco.

NUCLEAR POWER FIELD

Rochester, N. Y.—On Dec. 17, 1956, approximately forty-five members and guests of the *Rochester* Section attended a dinner meeting at the Liederkranz Club in Rochester, N. Y. Norman Block, consulting metallurgical engineer for the Foster Wheeler Corp., spoke on "Welding Problems Related to Specific Service Requirements of the Nuclear Power Field."

In his presentation, Mr. Block went into some detail about tube-to-tube sheet welding, stainless steel weld cladding and the maintaining of notch toughness in welded heavy sections of pressure vessels.

In particular, photographs and photomicrographs were shown demonstrating the various steps in the development of a stainless weld cladding process and an automatic tube-to-tube sheet welding process. Motion pictures as well as slides were used to give graphic evidence of the problems covered in the discussion.

The talk was well received by the interested laymen present, as well as by the welders and engineers.

Mr. Block, a graduate of the University of Illinois, with a degree in metallurgical engineering, did postgraduate work as a research assistant in the field of rail failures for the American Railroad Assn. In 1948, he joined Foster Wheeler as a research metallurgist and thereafter was promoted to chief metallurgist and then to position of consulting metallurgical engineer.

WELDING AND MANAGEMENT

Saginaw, Mich.—On Dec. 11, 1956, the *Saginaw Valley* Section enjoyed the largest dinner meeting of its history at the High Life Inn in Saginaw when the speaker of the evening was James F. Lincoln AWS, chairman of the Board of the Lincoln Electric Co. The theme of "Bring Your Boss" was well received with approximately thirty executives in attendance out of a total of over 150 members and guests. Mr. Lincoln's far-reaching prestige drew many guests and members from the Detroit area, including Ray Stitt, former national membership chairman, former District No. 6 director and present candidate for district representative on the National Nominating Committee; Keith Sheren, present District No. 6 director; Walter Kroy, recipient of the national award for obtaining the most new members, and Frank Boucher, Detroit area district manager for the Lincoln Electric Co.

Mr. Lincoln's nontechnical talk looked toward the vast future that

STUDENTS GUESTS OF PHOENIX SECTION

Phoenix Section played host to a number of students at the November 20th meeting. Standing are Instructor Paul Bell (*extreme left*) and Guest Speaker L. Marrell; students are R. P. Shaiyah, T. Peterson, John Leonard, Kenneth Langlois, J. E. Seris, Pete Fuller, Paul Kono, Willie Deal and Wisly Giles. Seated are (*left to right*) R. Smith, Steve Meehan and Paul Baum from the Section, and Instructor Riggans

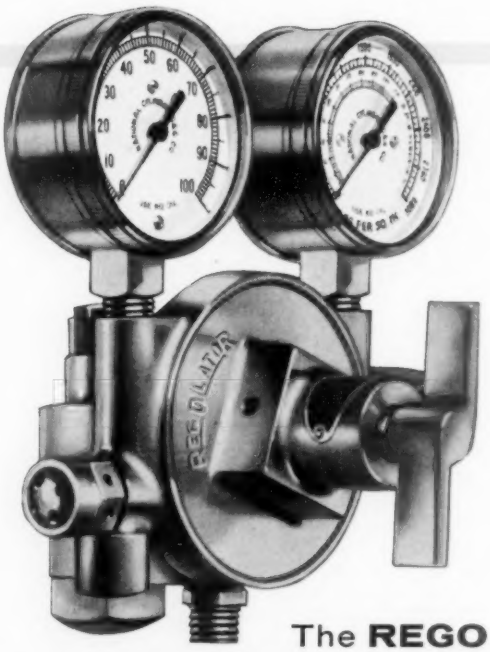
REGO*

Better Tools for Better Welding and Cutting

When you have Rego regulators, torches and tips, you get welding and cutting that excel in pace of production, in quality of welds and cuts, in freedom from the uncertainties and interruptions that so frequently hamper the operator's efforts. The human element, the fatigue factor, are minimized. *Every* operator becomes a *better* operator.

These better tools for better performance are built to last. Forty years of experience, of pioneering, of constant progress in design and manufacture stand behind Rego products and guarantee your complete satisfaction. See them at your earliest convenience, the sooner to start cashing in on increased production and economy. Try them in comparison with any others.

Your nearest NCG dealer or branch office will be glad to arrange a demonstration. Or write today for descriptive literature.



The **REGO**

Two - Stage Regulator

for stable, unvarying gas pressures

Two-stage construction. Balanced design using opposing valve characteristics maintains preset gas pressures unvaryingly through all stages of usable pressure. No more varying flames; no more running back and forth to make corrective adjustments. You set it and forget it.

Cartridge type sintered metal filter serves double purpose: keeps out destructive dust and dirt, lasts longer; prevents seat ignition by radiating recompression heat to valve body. Diaphragm mounting and bonnet have safety vents. Automatic relief valve protects from excessive pressure in first stage cavity. Forged bronze construction assures long life.

Models for standard duty, heavy duty and manifold service. Also single stage regulators.

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REGO GX Welding Torch

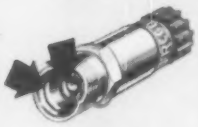
for ALL welding . . . from lightest sheet to heaviest casting

Superior design and painstaking manufacture account for the superlative performance and long life of the Rego GX. Precision adjustments, easily made, unvaryingly held, provide maximum flame efficiency. Free-flow mixer has proportioned and machined gas passages for perfect mixtures. Unique mixer design brings acetylene in through center, snuffs out fire in event of backfire or flashback. Mirror-burnished tip passages eliminate turbulence.

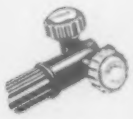
Fatigue factor minimized by tremendous weight-reduction. Handle and body made of lightweight yet strong aluminum alloy. So light and so perfectly balanced operators everywhere prefer the GX torch for this reason alone.

GX handles lightest sheet up to heavy castings. Operates at low pressures producing soft flames. Cylinder residuals reduced to a minimum. With KXA Cutting Attachment will cut steel up to 8" thick.

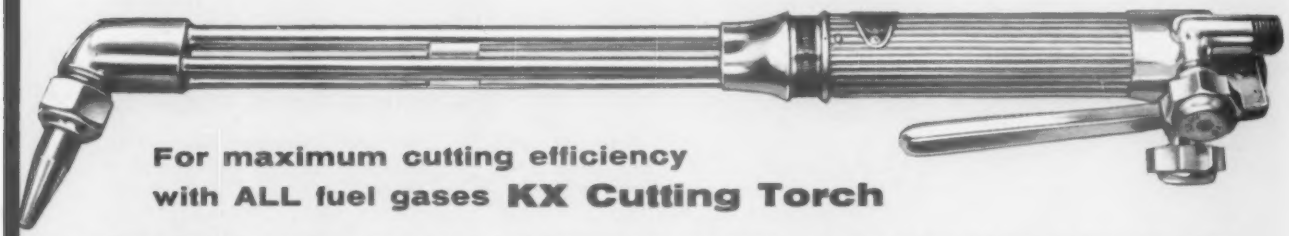
Leak-proof Joints . . . with Hand Tightening



Neoprene seating rings in the torch head permit positive, leak-proof coupling of mixer and handle with hand tightening alone. Result is longer torch life. No metal-to-metal contact. No strain on threads. Seating rings absorb normal wear and abuse, prevent scratching or scoring of metal seating surfaces.



Big, easy-grip valve wheels permit precision setting. Chrome-plated valve seats, monel valve stems. Non-seizing, non-scoring.



For maximum cutting efficiency with ALL fuel gases KX Cutting Torch

For toughest heavy-duty cutting of plate, multiple plate, slabs, sheets, and scrap. Ideal for use in shops, industrial plants, scrap yards, railroads—*wherever maximum cutting capacity is required.* Precision valve settings remain stable and uniform. Mixer delivers perfectly mixed gases. Perfectly proportioned and swaged tip passages provide non-turbulent preheat flames. Tip-mix type KX Cutting Tips—for cutting lightest sheet metal to heaviest sections—are available for use with any fuel gas.

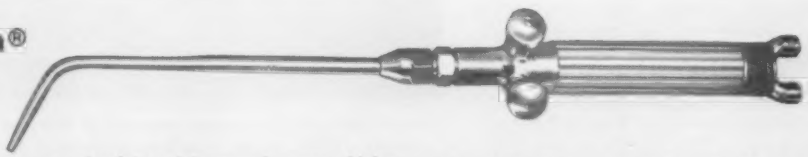
Diaphragm type high pressure valve in forged brass body replaces conventional packing to prevent leaks. Long high pressure lever gives operator precise control of cutting oxygen.

Stainless steel tubes in triangular construction give maximum strength and rigidity. Tubes silver-brazed at head and valve body to provide strong leakproof joints.

Stainless steel head withstands high heat without distorting, eliminates gas leaks that result from warped seating surfaces.

NCG 44 Bantam® Welding Torch

for LIGHT welding



The one light welding torch with all the features welders look for. Welds aluminum and alloys, aircraft tubing and steel from 28 gauge to ⅜" thick. It's light, easy handling, compact—*gets into an absolute minimum of space.* Valve wheels are right under your thumb for quick adjustment. Stainless steel stems are non-seizing and non-scoring. Nickel alloy tubes are silver brazed for added strength. Uses swaged-style Series NCG-71 Tips or screw-in style Series NCG-170 Tips. Length, less tip—6½".

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J. F. LINCOLN ADDRESSES SAGINAW VALLEY SECTION

Executives were plentiful at Saginaw Valley Section's "Bring Your Boss" night on December 11th. Guest Speaker James F. Lincoln (*second from right*) catches camera interruption while F. Boucher (*left*), R. Stitt and K. Sheren (*far right*) carry on their conversation

Mr. Lincoln's genial nature is caught in this picture as he listens intently to Bob Lachner (*center*) and Charlie Snow (*right*). Approximately 150 members and guests were present at the meeting. The affair proved to be a real success

lies ahead for the welding industry. He pointed out that the welding process is in its infancy with the young engineers of today holding the key to future progress in all phases of welding.

After his talk, a question-and-answer session was held, with many of the guest executives requesting Mr. Lincoln to specifically expound on the bonus arrangement at the Lincoln Electric Co., and the reasons why his company has been so successful in all phases of manufacturing and management, especially the so-called "employee-management" relationship. The highlights of the bonus plan were covered extensively with Mr. Lincoln pointing out that the key to his company's success is the attitude of everyone to want to work together to produce a better product, faster and at less cost.

PREHEATING FOR WELDING

Oakland, Calif.—Prof. Paul DeGarmo, of the University of California, addressed the *San Francisco* Section on November 26th at Plands Restaurant in Oakland on the subject of "Preheating for Welding."

Although preheating for welding has an impressive record, its use is lessened, he said, because a basic understanding of the mechanism by which it works is lacking. Data are scarce and full-scale tests are expensive, but results comparing 400° F preheated and ambient temperature manual welds allow the conclusion that, in low-carbon steel up to 1 in. thick and welded with E6010 electrodes, the properties obtained are as good for the preheated material as for the ambient temperature welds after stress relief at 1000° F. Preheating also has a marked beneficial effect in reducing underbead cracking of susceptible steels and, in the absence of notches, has about the same endurance strength as those stress relieved at 1200° F. The speaker also observed that preheated E6010 welds have as good transition temperatures as ambient-temperature E6016 welds, and the latter are not improved by preheat if proper moisture control is used.

He believed that improved performance of preheated welds is associated with the slowing down of the cooling rate around 400° F, which, by allowing the retained austenite to transform at a higher temperature, prevents cracking before harmful quantities of hydrogen have been eliminated from the welding metal. There is then, he postulated, less possibility of developing microcracking by atomic hydrogen encountering martensitic discontinuities at which microcracking starts.

The results for manual welds appear to bear out this theory. He has prepared a series of full-scale tests using the submerged-arc process which will be tested to check the theory for submerged-arc welds that microcracking occurs when microislands of brittle martensite are formed at low temperature in the presence of harmful amounts of atomic hydrogen. The tests include welds of ambient, preheated and chilled material made regularly and in the presence of hydrogen.

CHRISTMAS PARTY

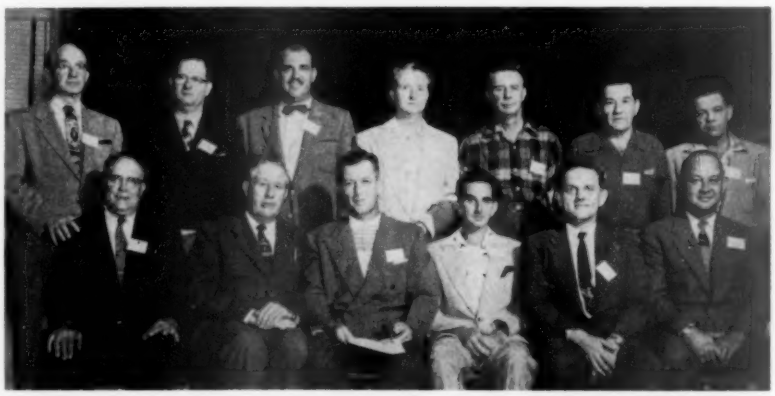
San Francisco, Calif.—The *San Francisco* Section held its first Christmas party on December 7th at the Lake Merced Country Club with 80 members and their ladies present.

Enjoying dancing to the music of Walt Tolleson and his rhythm-packed band between courses of the very delicious dinner served by the club, the group decided they had had a wonderful evening and expressed the hope that this was but the first of many successful parties.

AIRLINES PLANT TOUR

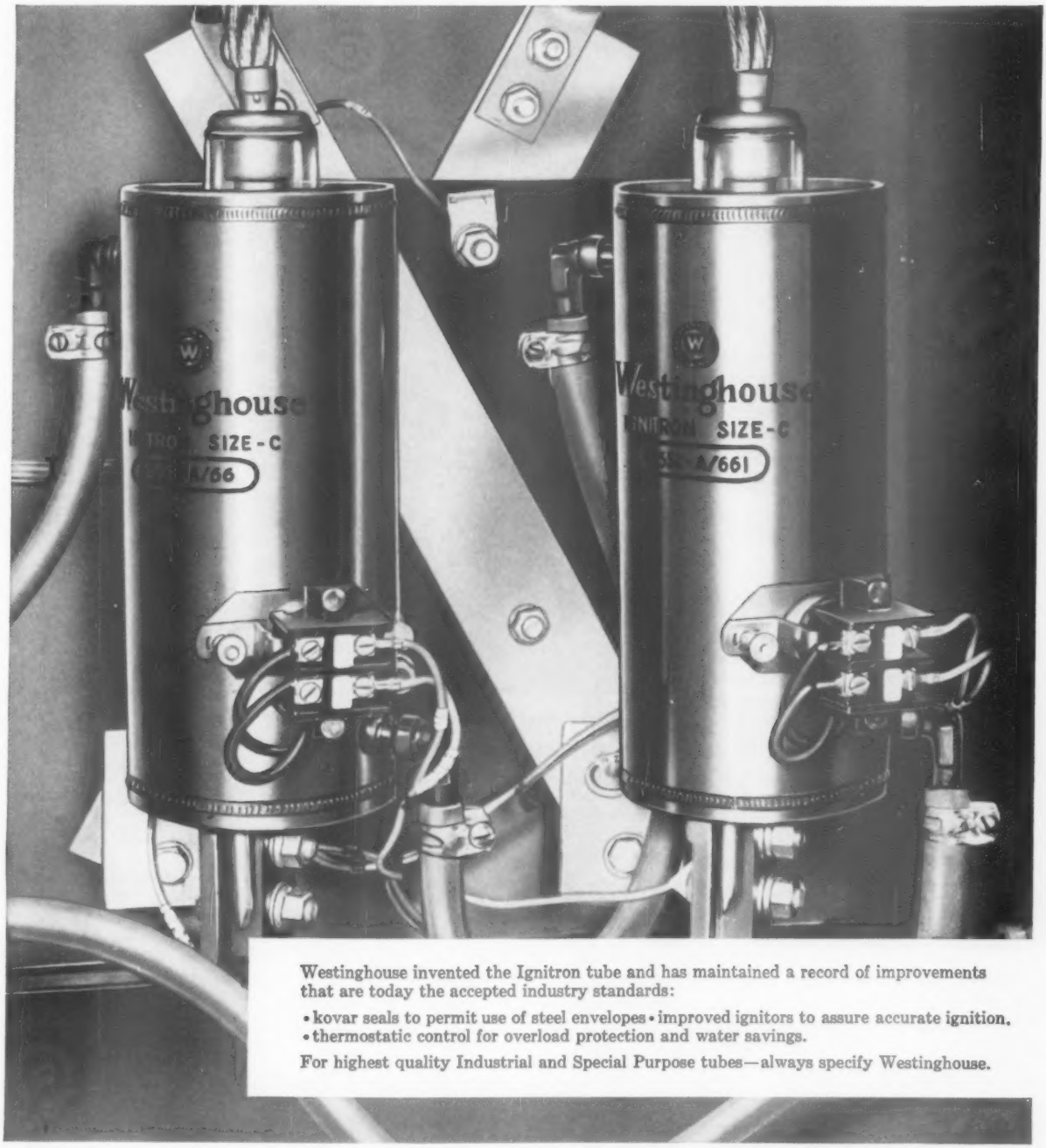
South San Francisco, Calif.—One hundred and twenty-five members of the *Santa Clara Valley* Section and their wives spent a most enjoyable evening on December 13th when they made a tour through the huge maintenance plant of United Airlines in

WITH SAN ANTONIO SECTION



Pictured above at a recent meeting of the San Antonio Section are (*standing, left to right*): F. W. Smith, publicity chairman; E. Wagner; C. Kinkae; C. V. Korn; J. S. Lowery, past chairman; A. J. Bell and C. W. Hornbeck. Sitting (*left to right*) are: C. E. Hosier, treasurer; R. Johnson, 1st vice chairman; R. Sprague, chairman; D. T. Hobbs, secretary; J. Rounds, 2nd vice-chairman, and V. E. Lane, director

Westinghouse **IGNITRON TUBES** still the industry standard.



Westinghouse invented the Ignitron tube and has maintained a record of improvements that are today the accepted industry standards:

- kovar seals to permit use of steel envelopes
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For highest quality Industrial and Special Purpose tubes—always specify Westinghouse.

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YOU CAN BE SURE...IF IT'S Westinghouse

South San Francisco.

First, a wonderful dinner was prepared at Bidou's Restaurant in San Jose and, then, the members and their wives left for United Airlines via chartered buses.

Upon arriving they were escorted in small groups through the immense maintenance plant where they were able to see the huge DC-7 airships being completely overhauled. It was difficult to imagine that these huge airliners could be taken apart and completely reconditioned from the huge stock of over 60,000 parts in approximately 7 days. Everyone in attendance was of the opinion that this was by far one of the most successful and enjoyable tours.

CARBON-DIOXIDE WELDING

Toledo, Ohio.—A meeting of the *Toledo* Section was held on Nov. 20, 1956, at the Maumee River Yacht Club. Following the dinner which was attended by 62 members and guests, Edward J. Vogel of the Liquid Carbonic Corp., Chicago, spoke on "Carbon-Dioxide Welding." Mr. Vogel discussed the basic problems concerning carbon dioxide in metal-arc gas-shielded welding, such as effects of moisture, gas control, manifolding, arc manipulation, wire analyses, joint design and weld position.

Mr. Vogel took a very new and controversial subject and covered it very well. He did not attempt to sell this type of welding as a cure-all, but carefully listed its advantages and disadvantages in a number of industries. He discussed the history of the process and the development being done for the future in plain-carbon steels as well as alloy steels.

Eighty-one members and guests attended the meeting and were all very pleased with Mr. Vogel's talk.

A special guest at this meeting was Keith Sheren, District No. 6 director, who spoke briefly on the aims of the SOCIETY and complimented the Toledo Section on its policy of arranging educational meetings.

NONDESTRUCTIVE TESTING

Toledo, Ohio.—There was a meeting of the *Toledo* Section at the Maumee River Yacht Club, on Dec. 18, 1956. Fifty members and guests attended the dinner and were entertained by slides taken at the November meeting and at the "Quiz the Experts" program held at Findlay, Ohio.

Eighty-one members and guests turned out to hear the main speaker, Prof. Robert C. McMaster (AWS), Professor of Welding Engineering, Ohio State University, speak on "Radiographic Inspection and Nondestructive Testing."

Professor McMaster began his discussion by covering a number of simple and complicated methods of nondestructive testing and listed the main advantages and limitations of each. He carefully pointed out that each test was only as good as the standard of calibration and each would only assure quality within its limitations.

The speaker then elaborated more on radiographic inspection, covering the ranges of a number of sizes of X-ray equipment, and various radioactive isotopes. Slides were shown of a number of welds as seen on the X-ray film and defects in the welds defined.

The general feeling was that Professor McMaster did a terrific job in covering the subject in a language that could be understood by operators as well as engineers.

Following the talk, a short film supplied by Arcos Corp., entitled "E. B. Insert Process," was shown.

DEGARMO ADDRESSES SAN FRANCISCO SECTION

Prof. Paul DeGarmo spoke on the subject of preheating for welding at the November 26th meeting of San Francisco Section. Sitting (*left to right*) are: C. B. Robinson, Professor DeGarmo, F. Stettner, S. Roberts and E. R. Babylon. Standing (*left to right*) are: W. F. Ajello, C. W. Doggett, D. K. Rowen and G. E. Watkins

Partial view of crowd in attendance at the meeting

ARC WELDING

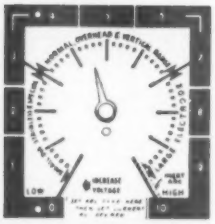
Grand Rapids, Mich.—The regular monthly meeting of the *Western Michigan* Section was held on Monday evening, December 17th, at Redwood Inn. The coffee talk was given by Lawrence (AWS) and Mrs. Grinstead on their hobby of copper enameling. They showed several samples of this work along with beautiful colored slides.

The technical session was very ably handled by William Wishart, chief metallurgist for Gibson Refrigerator Co. in Greenville, Mich. Mr. Wishart showed several diagrams to illustrate his talk on metal-arc welding

...for those tough "HARD to WELD" jobs

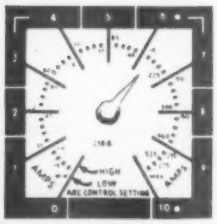
VICKERS Controlarc* DC Welder

Right at your fingertips—exact control—compact, light weight for truly versatile performance



SELECTION OF ARC TYPE

for different welding jobs, and adjustment necessary to suit your individuality and the welding job.



DUAL CONTINUOUS CONTROL

in a rectifier welder. Can vary the current from minimum to maximum, and vary the type of arc, with continuous stepless self-indicating dials.

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Gives You REMOTE CONTROL of CURRENT and TYPE of ARC

Even in those tight, cramped places, where there's hardly room to turn around, this handy little remote control box goes right with you. No matter what your welding position, you can "dial" the exact arc you need, without running back to the welder to change range switches and taps. Only the Vickers CONTROLARC gives you this individual control of current and type of arc in convenient remote control operation.

AND ONLY THE CONTROLARC GIVES YOU THESE EXCLUSIVE OPTIONAL FEATURES

Constant Potential (CP) Adaptor • Slope Controller

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for more information on this versatile welder.

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problems. A lively discussion followed.

MORGAN PLANT TOUR

Worcester, Mass.—The second plant tour of the season held by the *Worcester* Section took place on December 5th when the members of this Section visited the Morgan Construction Co. plant.

Richard L. Childs AWS, welding supervisor at the Morgan Construction Co., extended an invitation to the members to visit with him at the plant to which had been made a one and a half million dollar addition.

At the Morgan plant, the second shift was in operation and most of the machinery was being used, including heavy machinery, planers, millers, boring mills, gear cutters, etc. The Morgan Co. designs and builds rolling mills and wire-drawing machinery.

Mr. Childs led the visitation through the various parts and phases of the machine shop, and finally took the party to the Welding Department. Morgan does most of its own roll housing and machine base work. Some of these are made wholly as weldments; others are weldments with welded cast-steel bearing bases.

All those present agreed that Mr. Childs and the Morgan Construction Co. had been excellent hosts, and expressed their appreciation for a very pleasant evening.

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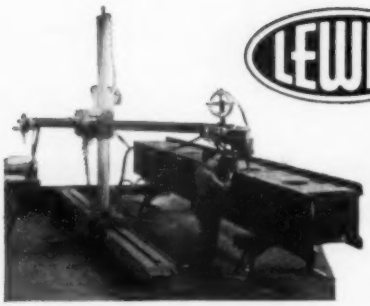

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For precision welding . . . plus portability . . . none can equal the LEWIS automatic welding head manipulator. Heavy-duty design and construction . . . machined and hardened ways . . . make the "Universal" both machine tool accurate and portable.

Once a "Universal" is positioned . . . a quick, easy job . . . it operates accurately and consistently. It releases valuable production floor space ordinarily consumed by a fixed installation. In addition, it will accommodate any standard submerged arc or inert gas-shielded arc welding head.

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ALL-NEW CATHODE ASSEMBLY

Brand-new cathode unit is supplied for each remanufactured G-E ignitron regardless of used-tube condition. Cathode parts get heavy wear, and are most likely to fail after long, hard service.

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STABILIZED-STEEL WATER JACKETS! ONLY G-E IGNITRONS HAVE THEM.

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General Electric welding ignitrons have two lives . . . can be rebuilt, save 28% on replacements!

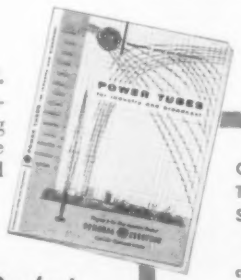
INSTALL General Electric welding ignitrons throughout your plant and you can reduce tube replacement expense by almost one-third. Because G-E ignitrons can be rebuilt to give a second complete service life . . . for 28% less money than new tubes cost.

When General Electric ignitrons near or reach end of life, hand them over to your G-E tube distributor. For each used General Electric tube, he will supply one completely rebuilt ignitron at savings in the range of $17.90 to $63, depending on size of the tube you exchange.

Rebuilt ignitrons last as long as new tubes, give the same dependable performance. They are factory-tested to meet new-tube requirements . . . carry the standard G-E ignitron warranty.

Thousands of dollars can be saved yearly when you install General Electric welding ignitrons—new and remanufactured. To accurately chart your potential savings in the year ahead, see your G-E tube distributor! He has all the necessary facts. *Electronic Components Division, General Electric Company, Schenectady 5, New York.*

"Power Tubes for Industry and Broadcast". ETX-10J. An up-to-date guide to characteristics and warranties for all G-E transmitting and other power tubes. Ask your G-E tube distributor for your free copy, or fill out and mail the coupon at right!



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A—Sustaining Member B—Member C—Associate Member

D—Student E—Honorary Member F—Life Member

Total National Membership

Sustaining Members	132
Members	5,758
Associate Members	6,151
Students	182
Honorary Members	8
Life Members	15
Total	12,246

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Chamberlain, Frank (C)
Hozier, Robert E. (C)
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Sisson, Howard A. (B)
Thomas, Donald L. (C)
Woodward, P. E. (C)

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Wicks, William P., Jr. (C)

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Carmean, E. R. (B)
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Wiggins, Frank L. (B)

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Mesick, Jack H. (C)
Puryear, John (B)

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Mucha, James J. (C)
Olsen, Raymond G. (C)
Pederson, Harold T. (B)
Perona, Louis A. (C)
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Warren, John J. (B)
Widner, Park (C)
Wolf, John J. (B)

CINCINNATI

Jahnke, Louis P. (B)

CLEVELAND

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Hazeldine, Arthur (C)
Straub, Arthur J. (B)

COLORADO

O'Hara, John W. (B)

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Fink, Robert K. (D)
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Lewallen, J. C. (C)

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Haynes, William C. (B)
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Weatherford, Carl E. (C)

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Schepper, Alva (C)
Wahl, Robert T. (C)

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Gibson, Norman V. (C)
Peterson, John D. (B)
Roberts, Kenneth A. (B)

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Koos, William J. (B)

KANSAS CITY

Beisel, Frank (C)

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Meyers, Charles A. (C)

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Applebaum, Harry (C)

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Pfeiffer, Casper (D)
Renouf, Roy R. (C)
Roeder, Edward R. (B)
Rohrberg, Roderick G. (B)
Shepler, Wilbur Seymour (B)
Tankersley, E. R. (B)
Winterhalter, Walter D. (D)

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Loapau, Vernie A. (C)
Todd, Edward (B)
Williams, Harold A. (C)

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Rogers, W. E. (C)
Williams, Kenneth Allen (B)

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MICHIANA

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Hess, Robert V. (C)

MILWAUKEE

Gulczynski, Peter F. (C)
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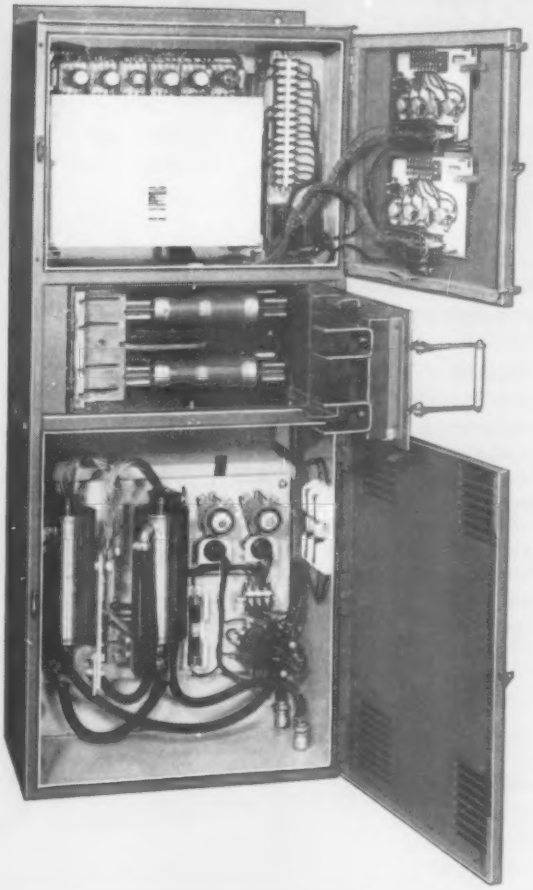

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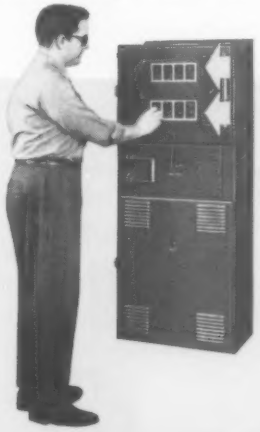

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Haidet, Lomar J. (C)
Kelly, Kenneth K. (C)
MacKusick, M. H. (B)
Robb, Henry Thomas, Jr. (C)
Zwahlen, Victor (C)

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Weaver, Edgar C. (C)
Winslow, Charles W. (C)

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TOLEDO

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Fisher, Charles L. (B)
Schwartz, Earl D. (B)

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Carrington, Arthur H. (B)
Chater, William (B)
Crisoliti, Urbano (B)
Meiller, Franz X. (B)
Pickles, Leslie (B)
Viticcioli, Fernando (B)
Weston, Frank (B)

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Cox, Wilburn C. (C to B)

LOS ANGELES

Johnston, Charles W. (C to A)

MADISON

Miller, Charles H. (C to B)

MILWAUKEE

Dyble, William J. (C to B)
Larson, Louis J. (B to A)

NEW ORLEANS

Evans, Joel C. (C to B)

NIAGARA FRONTIER

Kennedy, John P. (D to C)

PHILADELPHIA

Goehringer, E. E. (C to B)

SAN FRANCISCO

Robinson, Charles B. (C to B)

SANGAMON VALLEY

Baugh, Lester L. (C to B)

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For copies of articles; write directly to publications in which they appear

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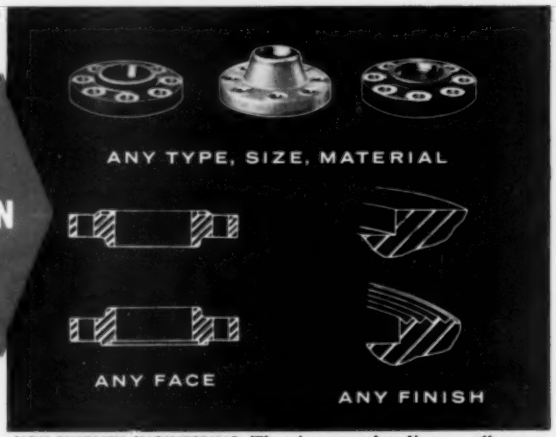

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NEWS of The Industry

RWMA Elects Uihlein President

At the December 1956 meeting, the members of the Resistance Welder Manufacturers' Assn. elected David V. Uihlein, president of Banner Welder, Inc., Milwaukee, Wis., as president of the Association.

At the same time, William H. Carrigan, vice-president and general manager of Kirkhof Manufacturing Corp., Grand Rapids, Mich., was elected to the vice-presidency.

The Resistance Welder Manufacturers' Assn. includes 18 of the foremost companies in the resistance welding equipment building field.

Taylor-Winfield Expands Plant

A half-million dollar plant addition to the Taylor-Winfield Corp., Warren, Ohio, resistance welder manufacturers, was completed Jan. 1, 1957, Vice-President T. S. Long has announced. It is the firm's seventh major expansion since 1945.

Cost of the new plant addition includes $204,000 for buildings and $340,000 for new manufacturing equipment. Mr. Long said this brings the total amount spent by Taylor-Winfield on plant expansion to $1½ million in the past 11 years. Other improvements have enlarged the firm's engineering laboratory research and office facilities.

The new construction includes two adjacent buildings with a total manufacturing space of about 30,000 sq ft. This is an increase in floor space of 35%. Machining, heavy fabrication and assembly operations will be assigned to these areas.

A. O. Smith Elects New Officers

The Board of Directors of the A. O. Smith Corp. has elected three new officers of the company, it was announced recently by L. B. Smith, president.

Named as vice-presidents were U. T. Kuechle, who has been automotive general sales manager since 1954, and Roy A. Dingman, who joined the company in April 1956 as director of industrial relations. Robert A. Rietz was named assistant secretary.

Mr. Kuechle joined A. O. Smith in 1929. He worked for a short time in the Personnel Department, then went into automotive sales.

Mr. Dingman was industrial relations manager of the Weyerhaeuser Timber Co., of Tacoma, Wash., from 1948 until he joined A. O. Smith. Prior to that he had been vice-president in charge of industrial relations for the Commonwealth Edison Co., of Chicago.

Mr. Rietz came to A. O. Smith in 1948 after graduating from Cornell University and taking postgraduate work at the University of Wisconsin.

Airco Acquires Jackson Products

J. H. Humberstone, president of Air Reduction Sales Co., a division of Air Reduction Co., Inc., has announced the company's acquisition of the assets and business of Jackson Products, Inc., of Detroit.

The Jackson Products Co. manufactures welding electrode holders, welding helmets, safety goggles and other welding supplies for the electric arc-welding field. Mr. Humberstone said that the present distribution channels will be maintained by Air Reduction Sales Co. He termed the transaction a further step in Airco's diversification program.

Mr. Jackson, who founded Jackson Products over 20 years ago, will continue his connection with the organization in an advisory capacity and A. C. Gilbert has been named general manager.

Cornerstone Laid for New Whitehead Building

The cornerstone for a new 35,000 sq ft office and warehouse for Whitehead Metal Products Co., Inc., was laid in Windsor on December 17th by Governor Abraham Ribicoff of Connecticut and T. M. Bohen, Chairman of the Board of Whitehead Metal. The building, to cost about $400,000, will be the initial unit in the first industrial park of its kind in the state.

The Whitehead company distributes aluminum, copper alloys, nickel alloys, stainless steels and plastics in the forms of sheet, rod, pipe, tubes and accessory items.

The building will be a one-story brick and aluminum structure. It will be open for business in June 1957.

Outlook for Welding Alloy Sales for Early 1957 Good

Sales of resistance welding electrodes and alloys are considerably higher than is customary at this time of year, according to reports from the industry. At the December meeting of the Resistance Welding Alloy Assn. in Detroit, manufacturers of electrodes and alloys indicated that business in the last quarter has been exceptionally good and the outlook for the early months of 1957 would appear to be very favorable.

The recently organized RWAA is actively engaged in the development of new and revised electrode standards. These standards are intended to aid the metal fabricating industries employing resistance welding processes to identify and select the proper electrodes for their production machines.

According to RWAA, prime users will be given an opportunity to review proposed Standards before their adoption by the industry to insure their greatest adaptability and the utmost service to customers.

COMING EVENTS

A Calendar of Welding Activity

AWS National Meetings

1957 Spring Meeting and Welding Show:

April 8–12, Hotel Sheraton, Philadelphia, Pa.

April 9–11, Fifth Annual Welding Show, Convention Hall, Philadelphia, Pa.

ASM

March 25–29, Tenth Western Metal Congress and Exposition, Los Angeles. Congress at Ambassador Hotel; exposition in Pan-Pacific Auditorium.

IAA

April 1-3, Fifty-seventh Annual Convention, Hotel Nicollet, Minneapolis, Minn.

INTERNATIONAL WELDING NEWS

IIW Bibliographical Bulletin for Welding and Allied Processes

The Documentation Commission of the International Institute of Welding (IIW) announces the availability of the Bibliographical Bulletin for Welding and Allied Processes. This Bulletin meets a general need for documentary information on the part of all those who are concerned with welding and allied processes. It is published with the financial assistance of UNESCO and has been warmly commended by it and by the Union of International Engineering Organizations, the latter having recommended its member associations to take the IIW's Bulletin as a model for any similar bulletins which they may publish.

As regards welding, this publication is reported to be the only one of this kind in the world.

The Bulletin contains systematic, clear, precise and methodical abstracts of books, news and articles appearing in the technical publications of the whole world on the various welding processes (electric arc, resistance, oxy-acetylene, etc....), oxy-cutting, surface hardening, metal spraying, building up, etc.... in all their applications. The permanent collaboration of the delegates of the various countries on the Documentation Commission of the International Institute of Welding has made possible the preparation of this bibliographical review. It has been edited, translated and published by the French Institute of Welding.

The Bibliographical Bulletin for Welding is published in two languages; one side of the pages is in French and the other in English, thus making it possible to keep a card index of the bibliographical abstracts in either language. The abstracts are classified in accordance with a specially devised system which is particularly adapted to welding documentation; the principles of the scheme can be rapidly learned from the classification table.

The annual subscription (four quarterly numbers containing more than 2200 abstracts) is as follows:

For the members of member Societies of the IIW (American Welding Society, Welding Research Council and American Ship Structures Committee): 4250 French Fr.

For other subscribers: 6400 French Fr.

Each new subscriber receives free of charge a copy of the classified table.

Subscriptions may be taken out direct from the publisher: "Les Publications de la Soudure Autogène," 39, rue d'Amsterdam, Paris 8°. •

Name Program Committee for Western Metal Congress

Charles W. Concannon, sales manager Absco, Inc., is chairman of a committee to formulate technical programs for sessions by the Los Angeles Section, AMERICAN WELDING SOCIETY, at the Tenth Western Metal Congress, March 25th to 29th, in the Los Angeles Ambassador Hotel.

Others on the committee are David P. O'Connor, senior machinist foreman, Department of Water and Power; John R. Ross, West Coast sales manager Silver Brazing Division, American Platinum Works, and C. B. Smith, super-

visor of Materials and Processing Engineering, Douglas Aircraft.

Other technical programs will be given by American Society for Metals, Society for Non-Destructive Testing and American Institute of Mining and Metallurgical Engineers.

Also on March 25th to 29th the Tenth Western Metal Exposition, with many exhibits of new welding technique, will be held in Los Angeles' Pan-Pacific Auditorium.

Thermacote Co. Moves Office and Plant

Robert Vaillancourt has purchased the Thermacote Co., formerly a subsidiary of Holiday Plastics, Inc., of Kansas City, and has moved the factory and general offices to 108 S. DeLacey Ave., Pasadena, Calif.

Two new branch offices have also been opened in the East, one at 430 S. Sixth St., Brooklyn, N. Y., and another at Atglen, Pa. (Philadelphia suburb).

Due to expansion of operations and the addition of several new products in the welding field, extensive manufacturing facilities were necessary.

The company's plant was formerly located in Kansas City.

Victor Builds New Plant

The new Victor Equipment Co., Alloy Rod and Metal Division, plant at Norwalk, Calif., represents approximately 40,000 sq ft of the most modern and up-to-date facilities for the manufacture of hard-facing rod and other products.

It features a method of straight-line production which is said to guarantee

absolute consistency. It has every modern innovation and equipment, together with highly mechanized production methods. All coating facilities for the manufacture of electric rods are completely air-conditioned and humidity controlled.

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Resistance Welding Equipment Activity Remains Strong

The regular monthly statistical data of the Resistance Welder Manufacturers' Assn., covering the first 11 months of 1956, has been distributed to member companies and it is now certain that both new business and shipments for the entire year will be very substantially in excess of any previous annual period.

At the end of November, new orders ran 27% ahead of the first 11 months of 1955. Shipments in this same 11-month period are 42% ahead of the similar period a year ago.

On November 30th, members of the Association reported a backlog of slightly less than $15 million.

Eutectic Announces Largest Profit Sharing Amount

Eutectic Welding Alloys Corp. has made its largest contribution to its Employee's Profit Sharing Plan and Trust since the inception of the program in 1953, according to an announcement recently by President Rene D. Wasserman. Although the exact amount was not disclosed, it was said that it ran into the tens of thousands.

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★ *Built-in Smoothing Reactor*... provides exceptional d-c arc stability.

Find out for yourself about the many additional distinctive advantages offered in the Silver W... circle the reader service number that appears below... J-21985

IF IT'S Westinghouse

Participation in the plan is extended to all executives and employees of the firm after five years of service. More than 25% of the personnel are already enrolled.

Eutectic's large contribution to the Trust is apart from and in addition to its yearly Christmas bonus policy.

Lincoln Pays Record Bonus

James F. Lincoln, Chairman of the Board of The Lincoln Electric Co., Cleveland, Ohio, manufacturers of arc-welding equipment, announces that the company's annual incentive bonus, paid Dec. 7, 1956, to its 1378 employees, is a record total of $7,425,000. Each employee's share is determined by a rating of his or her performance for the year. In addition, the company purchased approximately $800,000 of retirement annuities for employees.

In announcing the incentive bonus, Mr. Lincoln stated, "This incentive has been paid every year since 1934, and, since that time, we have paid a total of approximately $73,000,000 in bonuses and annuities, over and above usual earnings for similar work. This comparatively large amount, however, is not a generous gesture. It is something that is earned by our workers and its size directly reflects their efficiency."

DEDICATE SKYSCRAPER WITH FIRST "CORNERSTEEL"

The world's first "cornersteel" replaced the traditional cornerstone in the formal dedication of the new 45-story Socony Mobil Building. Made entirely of stainless steel, the chestlike cornerpiece was filled with mementoes of the event and sealed by inert-gas-shielded tungsten-arc welding. Watching welder Harold A. Roth are (*left to right*) Peter B. Ruffin, one of the builders of the structure; B. Brewster Jennings, chairman of the Socony Mobil Oil Co., Inc., for which the building is named; John W. Galbreath, Mr. Ruffin's partner; H. C. Turner, Jr., president of Turner Construction Co., the general contractors; J. H. Humberstone, president of Air Reduction Sales Co., and B. F. Fairless, president, American Iron & Steel Institute

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163	425	1200
175	438	1250
188	450	1300
200	463	1350
213	475	1400
225	488	1450
238	500	1500
250	550	1550
263	600	1600
275	650	1650
288	700	1700
300	750	1750
313	800	1800
325	850	1850
338	900	1900
350	950	1950
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Haynes Stellite Company designs and produces mechanized equipment for depositing hard-facing rods by the oxy-acetylene process. Machines are available for surfacing flat, longitudinal surfaces; flat, angled or grooved parts; and for handling cylindrically shaped parts. Write to any district office for more information on mechanized equipment and on the 19 HAYNES hard-facing alloys.

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PERSONNEL

Thompson Joins Fibre-Metal

J. R. "Joe" Thompson has joined the sales staff of the Fibre-Metal Products, Chester, Pa. His territory will cover the North Central West from North Dakota to Indiana and includes Iowa and Nebraska. Mr. Thompson will work with welding and safety supply houses on welding helmets and safety face and head protective equipment.

Haile Addresses NWSA

William M. Haile, vice-president and general manager of the Linde Air Products Co., was one of the principal speakers at the December 3rd meeting of the National Welding Supply Assn. in Birmingham, Ala. The title of his talk was "The Welding Supply Distributor—Yesterday, Today and Tomorrow."

Mr. Haile joined Linde in 1925 in the Southern Division office, Birmingham.

William M. Haile

He became assistant manager of the New York District office in 1935, district manager in 1937 and assistant eastern division manager in 1940. For three years beginning Sept. 1, 1942, Mr. Haile served with the War Production Board in Washington, D. C. He returned to the company in August of 1945 as sales manager for the Central Division in Chicago. He became assistant manager of this division on Jan. 1, 1946, and returned to Washington, D. C., for six months in 1951 to serve with the National Production Authority. On July 1, 1952, he was appointed manager of the Eastern Region in New York City and in January 1956, Vice-President and General Manager of Linde Air Products Co.

Richards Joins OKI Welding

James W. Richards has joined the sales force of OKI Welding Supply Co., of Cincinnati, Ohio. He will act as a specialist on the sales and service of gas welding and cutting equipment.

Mr. Richards was formerly owner of Richard Brothers Supply Co., at Youngstown, Ohio. Prior to that he was secretary-treasurer of welders sales & service at Youngstown, Ohio.

Mr. Richards started in the welding industry as a service operator in the Cleveland, Ohio, district for Linde Air Products. He was with that organization for $3\frac{1}{2}$ years before going in business for himself.

Quaas Appointed Vice-President

Joseph F. Quaas has been appointed vice-president in charge of manufacturing and production for Eutectic Welding Alloys Corp. according to an announcement by President Rene D. Wasserman.

Mr. Quaas joined the company in 1950. Before going with Eutectic, he spent 10 years in various capacities with one of the nation's large steel companies.

His previous work also included ex-

Joseph F. Quaas

tensive metal quality control and work on electrode coatings, as well as a period of years as research and development engineer for one of the country's largest electrode manufacturers.

Mr. Quaas is a graduate of Newark Tech with an A.E. degree in Chemical Engineering and has a B.S. degree in Industrial Chemistry from Newark College of Engineering. During his years of welding material development, he has served on various committees of the American Welding Society and the American Association for the Advancement of Science.

Rostoker Promoted

William Rostoker has been promoted to assistant manager of the metals research department at Armour Research Foundation of Illinois Institute of Technology, Chicago. He will be in charge of the applied metallurgy, foundry and steelmaking, physical metallurgy and welding sections.

Dr. Rostoker, who previously served as senior metallurgist and supervisor of physical metallurgy, joined the Foundation in 1951. Before that he taught at Illinois Institute of Technology and the University of Birmingham in England.

A native of Toronto, Canada, Dr. Rostoker received his bachelor and master of science degrees in metallurgy from the University of Toronto, and his Ph.D. in the same field from Lehigh University, Bethlehem, Pa.

The new assistant manager is the author of many professional papers and is a member of Sigma Xi.

Liechti Named Chief Engineer

Charles Liechti has been appointed chief engineer, Technical Services, of the Eutectic Welding Alloys Corp., Flushing, N. Y., according to an announcement by Rene D. Wasserman, president.

Mr. Liechti is a graduate engineer from Swiss Federal Institute of Technology in Zurich and has had many years experience in metallurgy and

Charles Liechti

metal fabricating. In addition, he was formerly the chief engineer of one of Socony Mobil Oil Co.'s Industrial Lubricating Divisions.

Mr. Liechti has just returned from a study of Eutectic's affiliated organizations in London, Paris, Lausanne and Frankfort.

EMPLOYMENT SERVICE BULLETIN

Positions Vacant

Salesman—Central and North New Jersey area. Submit written resume. Jersey Welding Supply, Inc., 234 East Third Street, Plainfield, New Jersey.

V-337. Excellent opportunity for editorial assistant-space salesman. National engineering society monthly requires resourceful, capable assistant to sell advertising space and to aid with over-all editorial and production activities. Field closely allied to metallurgy and metal fabrication. In reply please outline experience and personal history.

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ABSTRACTS OF CURRENT PATENTS

prepared by Vern L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington, D. C.

2,768,596—Fixture for Supporting and Cooling Articles During Brazing—Theodore W. Kalbow, Chicago, and Lester O. Reichelt, Naperville, Ill., assignors to Western Electric Co., Inc., New York, N. Y., a corporation of New York.

The fixture, in this instance, is adapted for supporting and cooling an article while a part is being brazed thereto. The fixture includes a pair of hollow cooling blocks having cooling surfaces conforming to portions of the article adjacent the zone of brazing. These cooling blocks are movable to and from article engaging positions.

2,768,597—Welding Fixture for Thin-Walled Tanks—George M. Smith, Grafton, Wis., assignor to A. O. Smith Corp., Milwaukee, Wis., a corporation of New York.

Smith's patent relates to a fixture adapted to position relatively thin side walls of a large rectangular tank in approximate positions for the welding of the longitudinal edges thereof. The article has one of these longitudinal edges being bent at a right angle to the body of the side wall with which it is integral to make possible a butt joint between the bent edge and the unbent edge generally adjacent thereto.

2,768,893—Brazing Alloys—Nikolajs Bredzs, Chicago, Ill., assignor, by mesne assignments, to Lithium Corporation of America, Inc., Minneapolis, Minn., a corporation of Minnesota.

This novel brazing alloy is self-fluxing and contains about 0.25% to about 8% lithium, from about 0.25% to about 2.5% boron, from about 5% to about 50% nickel, and the balance comprising essentially copper.

2,769,078—Arc Machining—Victor E. Matulaitis, Franklin, Mich., assignor to Elox Corp. of Michigan, Clawson, Mich., a corporation of Michigan.

This patent relates to apparatus for removing material from a workpiece by electrical discharge across a gap between an electrode and the workpiece. The apparatus includes means for limiting the flow of current through the electrode when the gap is bridged. Such means comprise a low-impedance electrical circuit connecting the power supply with the electrode and workpiece, and a high-impedance electrical circuit operatively associated with the low-impedance circuit. Other means that operate in response to a bridging of the gap so as to simultaneously disestablish the low-impedance circuit and establish the high-impedance circuit at such time.

2,769,079—High-Temperature Electric Torch—Rufus L. Briggs, Melrose, Mass., assignor to Amalgamated Growth Industries, Inc., New York, N. Y., a corporation of Delaware.

In this electric-arc torch, an upstream electrode, a hollow downstream electrode, and an arc chamber defining a passage between the electrodes and electrically insulated therefrom are provided. An inlet-gas chamber surrounds the upstream electrode and communicates with the arc chamber. The gas chamber is internally contoured to create a swirling action in gas introduced thereinto, and special means are provided for connecting an arc potential across the two electrodes.

2,769,080—Electric Welding Machine and Control Means Therefor—Jerome B. Welch, Wauwatosa, Wis., assignor to Cutler-Hammer, Inc., Milwaukee, Wis., a corporation of Delaware.

Welch's patented control relates to welding means and action including members responsive to the flow of welding current in the work for squeezing the work an amount depending upon the magnitude and duration of the welding current. The novel apparatus includes a welding transformer having a plurality of primary windings and secondary winding means for connection in circuit with the electrodes. Means in circuit with one of the primary windings are energized from one phase of a polyphase source of supply for supplying this one winding with a pulse of unidirectional current of one polarity from such one phase of current supply. Means are in circuit with the other of the primary windings to be energized from another phase of the source of electrical supply and they are rendered operative in response to flow of this above-mentioned unidirectional pulse of current from the one phase of the current supply. Thus a pulse of unidirectional current of the one polarity is also provided by the other phase of electrical energy supplied.

2,769,118—Automatic Arc Welder with Low Idling Voltage—Ray V. Lester, Kenmore, N. Y., assignor to Westinghouse Electric Corp., East Pittsburgh, Pa., a corporation of Pennsylvania.

The patented arc welding apparatus includes a welding transformer and a normally open main switch for connecting one secondary terminal of the transformer to an electrode conductor. A special auxiliary transformer is present in the circuit for supplying a hot-start impulse. Special connector and control means complete the welder apparatus and are responsive to special current flow to the electrode conductor for closing the main circuit switch.

2,769,226—Method of Welding Cylinder to Head—Ralph E. Van Deventer, Detroit, Mich., assignor to Studebaker-Packard Corp., a corporation of Michigan.

This patent relates to a method of fabricating a one-piece cylinder assembly defining a closed dome at one end of the combustion chamber for an internal combustion engine. In general, the process comprises the formation upon the cylinder head of an integral peripheral flange and the provision of a cylindrical barrel with a flange of predetermined length at one end thereof and effecting the desired welding action between these two members.

2,769,227—Welded Joint Between Ferritic and Austenitic Steel Members—Charles Sykes and Henry William Kirkby, Sheffield, England, assignors to Thos. Firth & John Brown Ltd., Sheffield, England, a British company.

The patented welded joint structure of this patent comprises a first member of ferritic steel having one thermal coefficient of expansion, and a second structural member of an austenitic steel having a different thermal coefficient of expansion. The joint is completed by a plurality of intermediate component layers of austenitic steel directly sandwiched between the first and second structural members. These intermediate component layers of steel are provided with thermal coefficients of expansion which increase in substantially equal steps from the coefficient of expansion of the first structural members to that of the second structural member.

2,769,888—Induction Means—Paul J. Abel, Cleveland, Ohio, assignor to the Yoder Co., Cleveland, Ohio, a corporation of Ohio.

Abel's patent relates to the continuous manufacture of welded tubing wherein the edges of a metal strip are closely opposed in a continuous longitudinal seam. Electric induction means induce heating current in the tubular blank and heat the opposed seam edge portions to welding temperature. Pressure means are provided at a closely following point for forcing the heated opposed seam edges together in welding engagement. A feature of the patent is that a web of nonmagnetic, electrically nonconductive, heat-resistant material is interposed between the tubular blank and the induction means.

2,769,231—Method of Joining an Aluminum Tube to a Steel Refrigerator Compressor—Leland H. Grenell, Oakwood, Ohio, assignor to General Motors Corp., Dayton, Ohio, a corporation of Delaware.

By this patented teaching, one end portion only of a steel tube is coated with aluminum, an uncoated end of the steel tube is silver soldered to a member such as a compressor unit, and thereafter an aluminum refrigerant passage line is inert gas welded to the aluminum coated portion of the first mentioned steel tube.

2,769,886—Induction Heating and Welding—Thomas J. Crawford, Berkley, Mich.

This apparatus is for the production of welded tubing from a tubular blank having an open longitudinal seam. The apparatus includes a heating coil adapted to have a blank moved therethrough and means adapted to force the opposed edges of the seam together at a point spaced beyond but adjacent the effective field of the coil. Special control means are provided for regulating the energy supplied to the heating coil.

2,769,887—High-Speed Induction Welding—Thomas J. Crawford, Berkley, Mich.

This patent relates to Crawford's Patent 2,769,886 and covers more control means and apparatus so that the induced current flow in the tubular blank being welded is concentrated in the opposed seam edges and at their point of juncture; the concentration of induced current and the substantially constant heat level produced thereby in the blank permits welding of the seam edges at high speed without stitching of such edges.

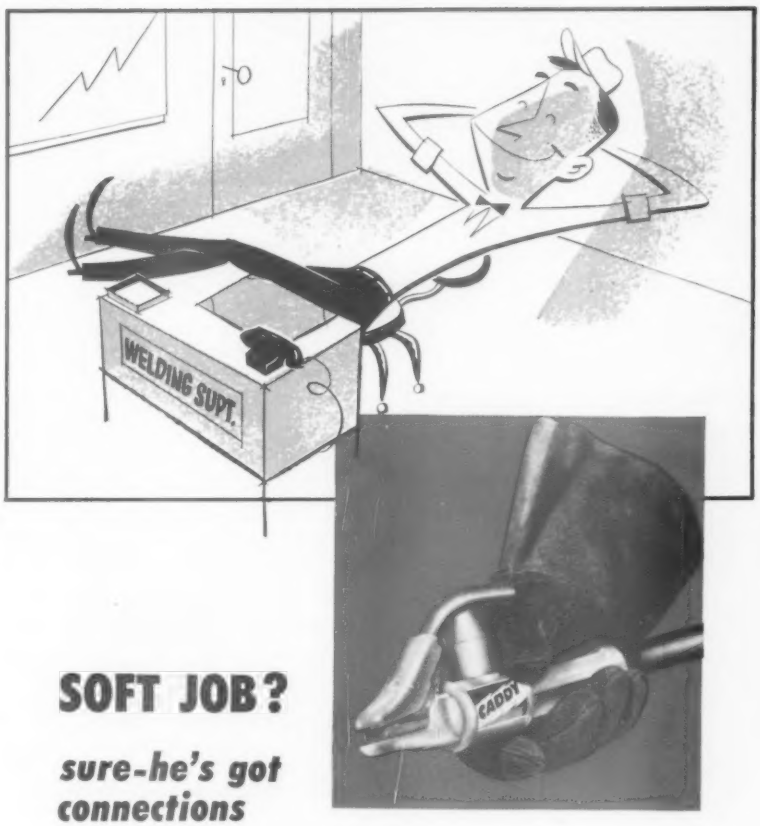
2,769,893—Electrical Device for Testing the Weldability of Metallic Materials—Josef Muller, Detmold, Germany.

This patented test device includes means for holding an electrode adjacent the specimen, which means include a clamp for the electrode, and a plurality of pins supporting the clamp and engaging the test specimen. One of these pins is adjustably positioned in the support means provided so that either the electrode, or such pin, can be in contact with the specimen and with the other such two members being spaced from the specimen.

2,769,894—Rare Gas Welding Torch—Walter L. Rives, Jacksonville, Fla.

In Rives' patent, the arc welding apparatus comprises a valve for controlling the supply of inert gas to the nozzle and electrode means provided. A contact member is provided for contact with a grounded object and special control means connect such contact member and other portions of the circuit to a valve for controlling the flow of gas in the circuit depending upon the welding current flow at any given time.

2,770,030—Welded Joint Between Dissimilar Metals—Otis R. Carpenter, Barberton, and Nicholas C. Jessen, Akron, Ohio, assignors to the Babcock & Wilcox Co., Rockleigh, N. J., a corpora-



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tion of New Jersey.

This patented weld joint is for use under cyclically variable elevated temperatures and pressures and includes one chromium-nickel austenitic alloy steel member and a low-alloy ferritic steel member of chromium containing type. The actual weld deposit for uniting such members consists of a ferritic alloy steel having substantially the same composition as the ferritic steel alloy member but containing a smaller amount of carbon.

2,770,708—Electric Arc Torch—Rufus L. Briggs, Melrose, Mass., assignor to Amalgamated Growth Industries, Inc., New York, N. Y., a corporation of Delaware.

Briggs' patented torch includes an upstream electrode and a hollow downstream electrode with an elongated arc chamber extending between the electrodes and axially spacing them. Inlet gas supply means are provided at the upstream end of the chamber for inducing a flow of gas along the chamber and through the hollow downstream electrode.

2,770,709—Electric Flash-Butt-Welding Machines—Charles D. Moore, Byfield, and Hermann W. Stieglitz, Marblehead, Mass., assignors to Thomson Electric Welder Co., Lynn, Mass., a corporation of Massachusetts.

This patented butt-welding machine includes relatively movable platen means controlled by reversible fluid motors. A snubbing device is provided in the apparatus for resisting the relative motion of the platen means during the first portion of the movement of the platen means toward one another and thereafter for permitting the control motor to complete the movement of the platen means toward one another substantially unresisted by the snubbing device. Other specialized control means complete the apparatus.

2,770,710—Arc Working Apparatus—Walter E. Burkart, St. Louis, Mo., assignor to Union Carbide and Carbon Corp., a corporation of New York.

Burkhart's patented apparatus includes an electric arc working torch including an electrode and a metal gas cup surrounding and spaced from the electrode and insulated from the body of the torch. Sources of working current and high frequency current are connected to the electrode. A feature of the apparatus is that a neon tube and a metal clip in the apparatus are connected to opposite terminals of the energy supply sources and that the neon tube and clip are effective as a potential discharge condenser to assure the starting of a working arc between the electrode and another electrode in circuit with the same sources of working current and high frequency energy.

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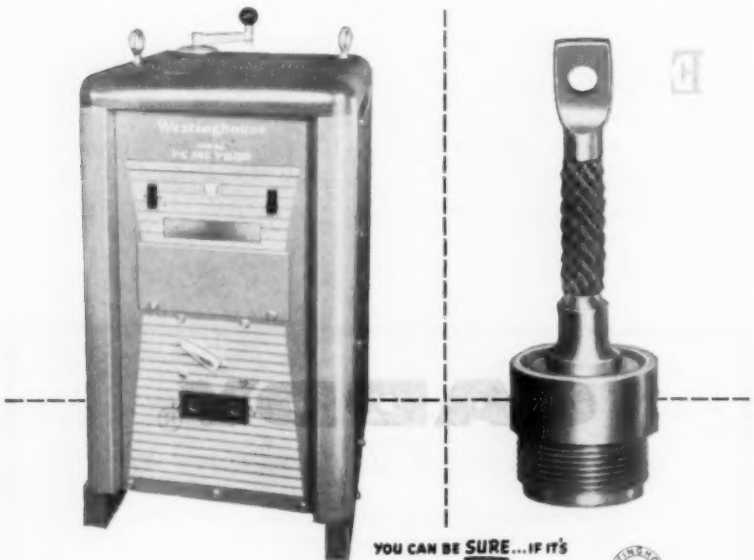
The new silicon rectifier is another milestone in welding, another Westinghouse *plus* that teams with "wide-amp" control and "arc-drive" control to bring you the *best* in welding equipment. J-21983



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2,770,871—Filler-Metal Piece for the Welding of Spheroidal Graphite Cast Iron—Roger Demalander, Pont-a-Mousson, France, assignor to the Compagnie de Pont-a-Mousson, Nancy, France, a French body corporate.

The present patent relates to a novel unit made from two cast-iron members united by welding and the cast iron members have spheroidal particles of graphite therein and the weld deposit comprises cast iron in which the carbon is grouped in the form of uniformly distributed spheroidal particles of graphite. As another feature, the micrographs of the cast iron of the deposit is identical to those of the cast-iron members. The patent also requires that the cast-iron members and weld deposit have specified compositions.

2,771,538—Welding Apparatus—Arthur L. Williams, Warren, Ohio, assignor to the Federal Machine and Welder Co., Warren, Ohio, a corporation of Ohio.

Williams' patent relates to a seam welder construction. In this welder, a shaft is journaled in a support and is provided with both rotational and axial movement. A weld wheel is fixedly secured to this shaft and moves therewith. Special means in the welder are provided for moving this shaft in an axial direction for distributing wear evenly across the face of the weld wheel

2,771,539—Apparatus for Welding Contacts onto Wire Spring Relays—Hubert A. Myers, Los Angeles, Calif., assignor to Western Electric Co., Inc., New York, N. Y., a corporation of New York.

In this instance, apparatus for welding contacts onto a plurality of springs of a relay part is provided. The apparatus includes pairs of welding stations, feed means for intermittently moving relay parts successively to and from the welding stations, and means for positioning the relay parts and the springs in a predetermined position at the welding stations. Other means feed a plurality of contact tapes onto the ends of certain of the springs at the different welding stations.

HOW THREE PHASE RESISTANCE WELDING IS USED TO FABRICATE STRUCTURAL STEEL

Production proves Sciaky techniques of resistance welding provide the consistently safe welds necessary for assembling primary structures.

For over 20 years resistance welding has been used to fabricate metal structures by many European contractors. However, until recently the process has been restricted to relatively light sections.

Structural Steel Fabrication

In Rouen, France, Enterprises Metropolitaines & Coloniales is now successfully resistance welding heavy structural beams into composite strength members.

Figure 1 shows a typical cross section of two "I" beams spot welded together. Figure 2 shows a more elaborate development of the assembly shown in Figure 1. Five "I" beams are welded into one composite strength member.

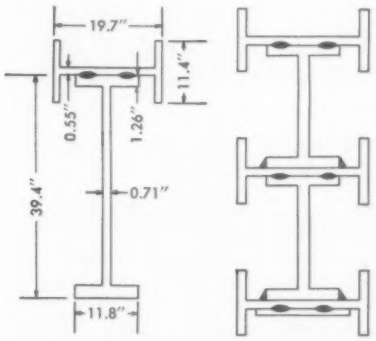


Fig. 1 Fig. 2

A typical beam is almost 69 feet long. It is part of an assembly 131 feet long weighing 45 tons. A total of 182 spot welds in two rows is used to fabricate the structure.

Sequence of Operations

To facilitate production, beam elements to be welded are mounted on an arrangement of two carriages located in front of the welder. One of the carriages is powered and operated by remote control by the single operator (not skilled) who also triggers the weld sequence. Indexing time is 4 seconds.

The welding of the assembly is accomplished in four operations. Operation No. 1 begins in the middle of the column. One row is made to the end and the welder returns to the middle to weld the balance of the row in the other direction. The second row is accomplished in the same manner.

The average time required for each spot weld including indexing is less than 40 seconds. *The entire beam is fabricated in less than two hours including set-up time.*

The Welder

The welder is a Sciaky Patented Three Phase 450 KVA Welder designed to be operated from a cradle or in a suspended position (as shown in the photograph). Power supply is 220 volts. Maximum electrode force is 14 tons. Maximum secondary current is 75,000 amps. Controls are located directly opposite the welder.

The key to successful welding of heavy structural shapes is the Sciaky Patented Three Phase Principle. In addition to its many other advantages it provides the weld quality and consistency essential to primary structure fabrication.

Advantages of Resistance Welding

The use of Sciaky Resistance Welding Techniques results in a substantial saving in material and weight.

It eliminates extra beam size necessary to compensate for the weakening effect of rivet holes. The extra operation and time necessary to drill rivet holes is eliminated. In addition the material and weight contained in rivet heads is eliminated.

Only one worker (not skilled) is needed for the welding operation.

Further Information

More detailed information on this operation is presented in a twelve page illustrated report. Write for your copy, asking for the *"Resistance Welding at Work Special Report on Fabricating Structural Steel."* There is no obligation.

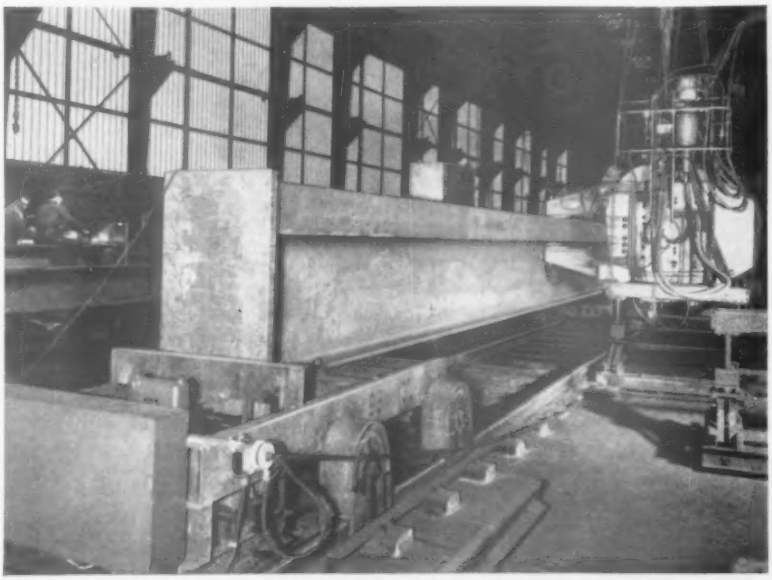


Fig. 3 View of beam riding on powered dolly as welds are being made by the Sciaky Patented Three Phase Welder.

Largest Manufacturers of Resistance Welding Machines in the World

SCIAKY®

Sciaky Bros., Inc., 4919 West 67th Street, Chicago 38, Ill., Portsmouth 7-5600

NEW PRODUCTS

Resistance Welding Controls

Two new synchronous timers and two new thyratron contactors available from Raytheon Manufacturing Co., 100 River St., Waltham 54, Mass., are designed to assure precise control of current duration and magnitude for accurate, uniform resistance welding.

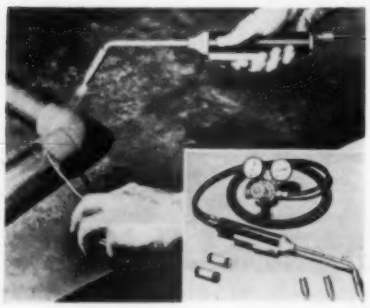
The synchronous timers are offered in two models—Model 2–152 Single Synchronous Timer; and the Model 2–153 Dual Timer. The single synchronous model has two independent control knobs—one for heat adjustment and one for time. The dual unit has two independent controls for heat and two for time, permitting the use of two separate welding heads or a dual head.

For complete details, write to Raytheon Manufacturing Co. at the above address.

Acetylene Torch

An acetylene torch which lights automatically when the operator presses the control lever and shuts off instantly when not in use has been developed by the Velocity Power Tool Co., 201 N. Braddock Ave., Pittsburgh.

According to manufacturer, the slender, well-balanced Torch-O-Matic is the first acetylene torch that lights automatically from battery-powered ignition. It is said to combine safety and economy in a torch which can be used for dozens of heating, soldering, sweating and brazing operations.



Pressing the lever simultaneously opens the torch gas valve and creates an electrical spark, which ignites the gas. The instant ignition eliminates time-consuming and hazardous manual lighting and adjusting operations, for greater economy of operation.

For further information, write Velocity Power Tool Co., 201 N. Braddock Ave., Pittsburgh 8, Pa. Ask for Bulletin No. 1413-7.

Portable X-Ray

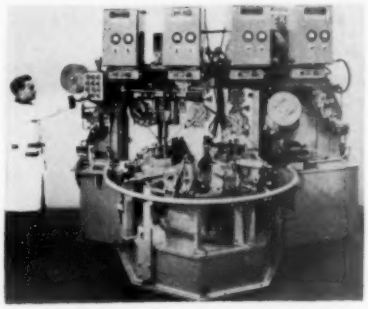
A 130-kv portable X-ray with tube head weighing only 50 lb and capable of penetrating up to $1^1/_4$-in. steel has been announced by Andrex. Tube head measures only $7^1/_2$ x 18 in. and operates on a simplified, lightweight control unit of its own or on a standard Andrex 160-kv control. This new 130-kv unit, especially suitable for spot x-raying of welds on storage tanks, pipe lines and other structures supplements the Andrex line of 160 kv, 200 kv and 260 kv X-ray units.

For additional information, write to Holger Andreasen, Inc., Dept. 130, 703 Market St., San Francisco, Calif.

Automated Index Welder

A new automated 8-station horizontal rotary index arc-welding machine designed for accurately positioning and welding together two-piece production parts is now available from Expert Welding Machine Co., 17144 Mt. Elliott Ave., Detroit 12, Mich.

The machine features eight 2-stage clamping fixtures, which are located at each table station. One fixture stage accurately positions and clamps a two-piece part while tack welding operations are performed. The second stage fixture clamps and rotates the tack-welded assembly, while continuous contour welds are made on both sides of the part. The index table is driven by an Expert standard cam index mechanism which decreases cycle time, provides exceptionally smooth index movement and simplifies over-all machine complexity.

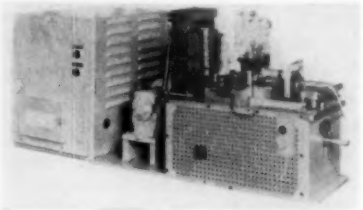


For complete details write directly to manufacturer at the above address.

Automatic Wire Cloth Welding Machine

A new, completely automatic machine for forming and welding cylindrical screens of wire cloth has been introduced by Federal Tool and Engineering Co. (Tweezer-Weld), of Cedar Grove, N. J., manufacturers of precision welding equipment for specialized applications.

Already being used in the automotive industry for the mass production of gas line filters, the new welding machine is expected to find wide use also in the aviation industry for the making of fuel line strainers and screens, and in the electronics industry for forming and welding vacuum tube and microwave parts.



For further information and specifications without obligation, write directly to R. H. Pityo, Federal Tool and Engineering Co., 1384 Pompton Ave., Cedar Grove 1, N. J.

100% X-rayed...100% X-ray sound

Welded with

ATOM·ARC 8016 CM

Welded by

Baldwin-Lima-Hamilton Corp.

Eddystone Division

Albert Imburgia, Welding Engineer, explains attachment welding detail on underside of reactor. Plate material is A301-54T-Gr. B firebox quality steel. Fittings are A 301-54T-Gr. B, A 335-53T-Gr. P12, A 182-53T-Gr. F12. Thicknesses ranged from 2-5/8" to 5-1/16".

100% X-ray sound with no rewelding is a common occurrence on Atom•Arc applications. It's also a common occurrence to find the country's leading fabricators switching to Atom•Arc electrodes for welding jobs involving mild and low alloy steels. One such company is the Baldwin-Lima-Hamilton Corp. of Eddystone, Penna.

Frank Iapalucci, Manager of Welding Engineering, and Carl Ascenzi, Chief Welding Engineer, have developed at Baldwin a quality control program for weldments that demands the ultimate in performance for every electrode used in their shops. Atom•Arc 8016 CM electrodes were their choice for all hand welds on this reactor for a large eastern refinery. The X-ray results proved their judgment sound.

Aside from the fact that no rewelding was required, the greater welding speed and easier operation of Atom•Arc electrodes were a great help in completing the job on schedule.

Atom•Arc electrodes will provide X-ray quality weld metal and greater joint efficiency, at a lower cost, on a wider range of ASTM steels than any other single group of electrodes. Atom•Arc electrodes are available in the following AWS grades: E7016, E7016 A-1, E8016 C-2, E8016 B-2, E9016 B-3, E10016 (1.70% Mn, .40% Mo), E12016 (1.9% Ni, .9% Mo, .2% V) and Atom•Arc T (E11016) for welding T 1 steel.

Find out how the savings and quality of Atom•Arc electrodes can benefit you. Contact your Alloy Rods Company distributor or write for informative data sheets.

Alloy Rods Co.

General Offices and Plant
Lincoln Highway West
YORK 3, PENNSYLVANIA

Pacific Coast Sales
Office and Plant
750 Lairport Street
EL SEGUNDO, CALIF.

no finer electrodes made...anywhere

X-Ray Equipment

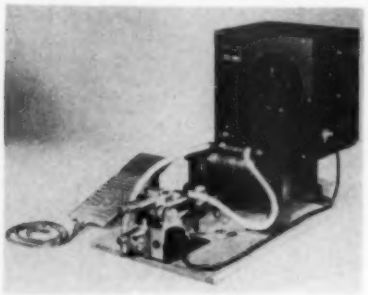
Periodic inspection of cylinders to confirm soundness of welds over engine cracks is now accomplished by portable X-ray at Pan-American World Airways,

San Francisco. According to Holger Andreasen, Inc., 703 Market St., San Francisco, Calif., the compactness and mobility of the Andrex portable equipment has extended the use of X-ray to examination of radiators, bank manifolds, rocker box covers, harness and fuselage structures. Andrex operates anywhere on the base on any 110 v or 220 v supply without special transformer. Can be easily airlifted anywhere.

For further information write directly to Holger Andreasen at the above address.

Welding Head

Federal Tool Engineering Co. of 1390 Pompton Ave., Cedar Grove, N. J., recently announced that their single



electrode welding head, model TW-1 is now offered in a complete welding unit, including a 1-kva a-c transformer, heat control, timer and foot switch.

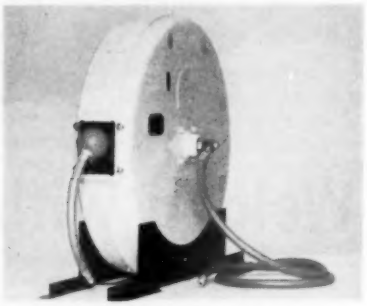
Designed for production hand welding of small parts, the TW-1 package unit finds extensive application in the electronics and allied industries for the microscopic resistance welding of vacuum tube elements, transistor parts and subminiature components.

For further information, write directly to Sales Manager, Federal Tool Engineering Co. at the above address.

Hose Reel

United Specialties, Inc., P. O. Box 698, El Dorado, Ark., manufacturers of industrial hose and cable reels under the trade name "Weldreel" announce a new single hose reel added to the line.

The new unit features a recently designed swivel using Chevron type seals with sintered bronze graphite bearings in the stainless steel spindle; a steel



shaft rotating on ball bearings; a long life clock type motor spring; adjustable mounting brackets for installation in several positions; a lock pawl device with easy release.

For complete details write to manufacturer at above address.

Mild-Steel Electrode

A new manual-arc welding electrode, recommended for general-purpose applications and classified AWS E6013, has been announced by the General Electric Co.'s Welding Department.

The new electrode, with General Electric designation W613B, is heavily-coated mild steel, designed to operate on either alternating or direct current, in all positions. The moderate arc force is sufficient to give adequate penetration, according to company engineers, and the steady spray type transfer aids in control and direction of the deposit.

The molten deposit reportedly wets and spreads easily, so that welds have uniformly feathered edges and good shape. The slug produced is said to be such that flat beads are free of depressions, and removal is completed easily. An outstanding characteristic of the electrode is its particular suitability to vertical fillets welding down. Still another special characteristic is the combination of toughness with ductility found in the metal deposit.

For complete details, write to General Electric Co., Welding Department, Schenectady 5, N. Y.

AT THE FRONTIERS OF PROGRESS YOU'LL FIND

FOR WELDING AND CUTTING...

IN THE RED DRUM

HIGHEST QUALITY

DUST-FREE

AN AIR REDUCTION PRODUCT

NATIONAL CARBIDE

Write for the name and address of the NATIONAL CARBIDE supplier nearest you.

National Carbide Company

GENERAL OFFICES: 150 EAST 42ND STREET, NEW YORK 17, N.Y.

A DIVISION OF AIR REDUCTION COMPANY, INCORPORATED

"New Lamps for Old?"

As in Aladdin's day, you hear the cry "New Lamps for Old!"

The trade-in or rebuilding deals offered on various types of electron tubes is a current example.

AMPEREX *cannot use your old welding ignitron because*
EVERY AMPEREX TUBE IS A NEW TUBE MANUFACTURED WITH ALL-NEW PARTS AND SOLD AT THE SAME FAIR PRICE TO ANY USER, WHETHER HE HAS AN OLD IGNITRON TO TRADE IN OR NOT!

AMPEREX *ignitrons are never rebuilt tubes.*
Instead of "new-for-old" deals...
AMPEREX OFFERS A QUALITY WELDING IGNITRON WITH GREATLY EXTENDED LIFE AND BACKS THIS UP WITH THE LONGEST SERVICE-PERIOD WARRANTY IN THE ELECTRONIC TUBE INDUSTRY — 2 FULL YEARS!

Remember, your real ignitron cost is your cost per hour of actual use!

AMPEREX IGNITRONS FEATURE.

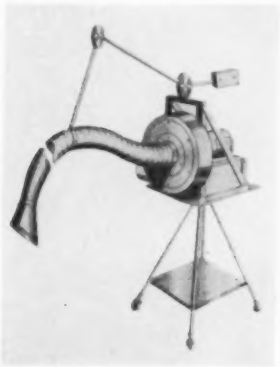
- Exclusive "long-life" ignitor— promoting ease of firing
- Practical thermostatic control
- Visible glass anode seal— simplifying observation of firing
- Insulating shield around ignitor terminal
- Heavy, removable, braided, copper anode lead *(Also available in plastic coated models)*

Write directly to factory for data sheets and name of your nearest AMPEREX distributor.

Amperex ELECTRONIC CORP.
230 Duffy Avenue, Hicksville, Long Island, N. Y.
In Canada: Rogers Majestic Electronics Ltd.
11-19 Brentcliffe Road, Leaside (Toronto 17)

Fume Exhausters

Engwald Corp., 357 Lafayette Ave., Brooklyn 38, N. Y., announces the availability of the W-200-50 stand-type exhausters. Movable on steel

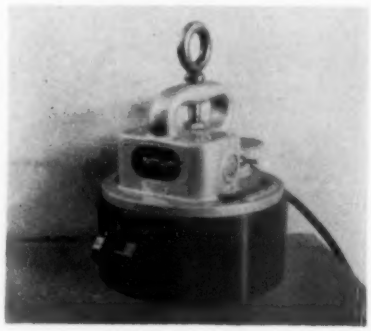


casters, these units are specially designed to go directly close-up to the fume site—apply a very high suction—and then push the fumes to the nearest opening to the external atmosphere.

The Engwald W-200-50's comprise gasoline or electrically driven blowers capable of moving up to 800 or more cubic feet per minute (depending upon individual needs), plus 8-ft stainless steel hose assembly including arms and counterbalance for universal positioning. Available hose diameters are 4, 6 and 8 in. Write directly to Engwald, address above, for more information.

Magnetic Plate Clamp

A new model of their portable electromagnetic plate clamp for positioning and holding work for welding has been announced by Buck Mfg. Co., of Los Gatos, Calif. The PC-6, as the new unit is called, weighs only 78 lb and



stands just 10 in. high (compared to earlier models which were 16 in. tall). Performance features include direct lift off up to 6500 lb; and a reverse polarity switch for quick release of magnetic pull (there is no time-wasting lag), according to manufacturer.

For complete specifications and prices, write to Buck Mfg. Co., 100 Roberts Rd., Los Gatos, Calif.

Stainless-Steel Electrodes

Two new series of coated stainless electrodes have been developed by the Arcos Corp., Philadelphia, which produce high quality welds with unusual operating ease, according to the manufacturer.

One, known as Aerocor, is designed especially for welding aircraft and light-gage stainless steel. The other, called Fabricor, is intended for welding stainless plate. Both are applied with d-c reverse polarity.

The Aerocor series is reported to produce sound welds with the right contours and good appearance. The new Fabricor electrodes are said to be distinguished by their excellent arc action, no undercutting, easy slag removal and ability to produce top quality welds.

Once the arc is established with either type electrode, it is possible for the operator to use a "drag-type" welding technique.

Aerocor electrodes are available in four sizes ranging from $^1/_{16}$ through $^1/_8$ in. The Fabricor series is available in five sizes ranging from $^3/_{32}$ through $^1/_4$ in.

For complete details write directly to Arcos Corp., Philadelphia, Pa.

General-Purpose Electrode

A new general-purpose electrode which meets the AWS and ASTM E6013 classification is now available from Air Reduction Sales Co. Designated Airco 90B, it is a heavily coated electrode of moderate arc force designed to operate in all positions on alternating or direct current.

Airco 90B is particularly useful for welding mild steel to medium-carbon and low-alloy steels. In addition to high mechanical and impact properties, the finished bead is reported to be free of surface depressions common to this type of electrode.

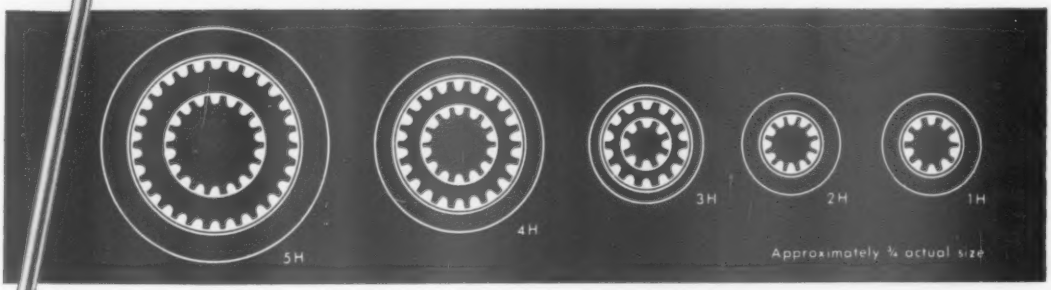
SUPER HEAT

From a Super HEATING-TORCH

The HARRIS K-23

Where intense heat is required, hundreds of metal working plants have found the answer in the Harris K-23 Super Heating-Torch.

Designed by Harris engineers for either Propane, city or natural gas in combination with oxygen, you can do your heating job faster, better and more economically than by any other method.



The scientific design of the Harris 2290H multi-flame tips practically eliminates heat loss.

(Torches for natural gas and propane, with compressed air, also available. Information on request.)

HARRIS

HARRIS CALORIFIC CO.

5501 CASS AVE. • CLEVELAND 2, OHIO

Additional information on the new Airco 90B electrode can be obtained by writing to Air Reduction Sales Co., a division of Air Reduction Co., Inc., 150 E. 42nd St., New York 17, N. Y.

Taper-Face Flange

The recently introduced Tube-Turn 125-lb taper face light-weight welding neck flange offers important technical and cost advantages where the lighter schedule of pipe must be connected to 125-lb cast-iron valves and other equipment. It is manufactured by Tube Turns, Louisville, Ky., a division of National Cylinder Gas Co.

The flange, on which a patent is pending, is reported to overcome two difficulties. First, its tapered face causes greater pressure to be exerted near the

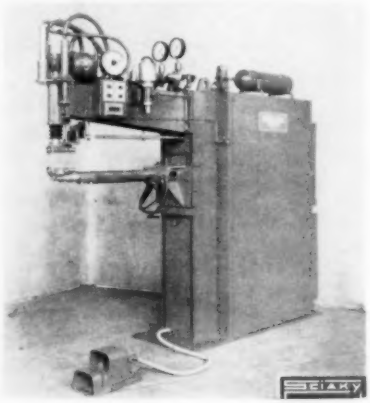
bore. Second, the new flange, equipped with a full face gasket, can be safely bolted to cast-iron or semisteel flanges. Also, when the new flange is properly employed, the joint is capable of withstanding approximately three times the pressure it could contain if the taper was omitted.

The new flange is rated at 125 lb for water, oil and gas service. It matches 125-lb cast-iron flanges and valves made to ASA B16.1, and the bolt circle and drilling are the same as employed for ASA 150-lb standard steel flanges.

For complete information, write directly to manufacturer at above address.

Spot Welder

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., announce the new design of a competitively priced, special, air-operated, electric-resistance press model combination seam and spot-welding machine. As a seam welder, the upper and lower heads are rotatable to either side to permit either transversal or longitudinal seam welding.



The PMM.OC-50-36 is especially designed to seam and spot weld light gage metals to Military and Aircraft Specifications.

For more complete information about the PMM.OC, write Public Relations, Dept. M-13, at the Chicago address.

Soldering Iron

The Prest-O-Lite 406 medium soldering iron, a new air-acetylene unit designed to handle all but the heaviest

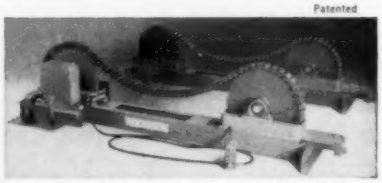
soldering jobs in sheet metal, electrical, plumbing and plant maintenance shops, has been introduced by Linde Air Products Co. Operating on clean-burning acetylene, the 406 heats up to full working temperature in less than two minutes, and will do the work of a pair of old-fashioned furnace-heated 3-lb irons or a 880-w electric iron.

With its form-fit handle and convenient thumb rest, the 406 is said to provide a firm, comfortable grip and

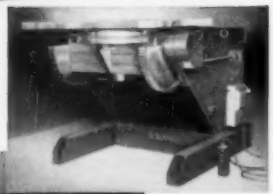
Aronson

Offers You Over 200 PROVEN Standard Stock Models of Quality POSITIONERS "POSITIONEERED" to your exact Requirements

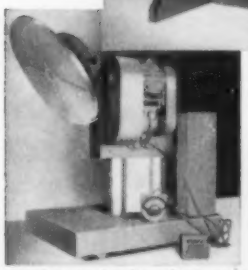
Patented



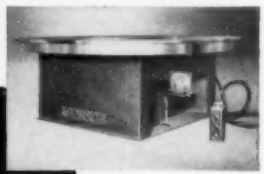
Aronson TracTread (T. M. Reg.) Turning Rolls for thin-walled heavy cylindrical work to 27 tons capacity. Zero to 100 IPM turning speed and Built-In Grounding.

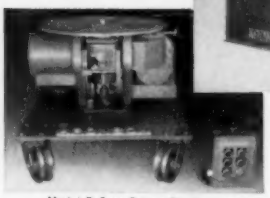


Heavy Duty Precision Built Rubber and Steel Tired Turning Rolls, 100% overload protected. Capacities to 250 Tons. Also Tilting Rolls, Pipe Rolls and Rail Cars.



Heavy Duty Gear Driven Positioners, with Magnetic Braking, Mercury Grounding, and Optional Speeds. Capacities to 24,000 lbs.

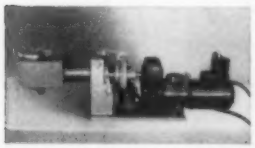




Heavy Duty Floor Turntables with precision speed control and Magnetic Braking, used for welding, burning, X-raying, etc. Capacities to 75,000 lbs., various heights and speeds.

Fully Automatic Gear Driven Positioners, featuring Power Elevation, Tilting and Rotation. 5,000 lbs. Capacity.

Model D Gear Driven Positioners, Compact, Precise, Rugged. Capacities to 1000 lbs.



Bench Turntable Automatic Positioners with Mercury Grounding Capacities to 500 lbs.

Patented

Aronson Universal Balance Positioners (T. M. Reg.) position your weldments effectively, instantly for downhand welding. Capacities to 2000 lbs.

Rugged Head and Tail Stock for positioning bulky weldments between centers. Table Backup for Zero Deflection. Magnetic Braking. Capacities to 160,000 lbs.

Aronson Twinner Permanent Magnetic Clamps, six models with thousands of uses in welding set-up work. Write for bulletin.

Quality PRODUCTS by Aronson MACHINE COMPANY
ARCADE, NEW YORK

WELDING SUPPLIES • RADIOGRAPHIC EQUIPMENT
PLATING MATERIALS • ORGANIC COATINGS
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Welding Progress

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IMPROVEMENTS IN HARD SURFACING MADE WITH LOW HYDROGEN ELECTRODES

Hardex Electrodes decrease porosity, penetration, and weld metal dilution, increase resistance to cracking.

Metal & Thermit has recently introduced a new line of electrodes for hard surfacing. Low hydrogen coatings have been adapted to these HARDEX hard surfacing electrodes—a new and different approach to improve the quality and wearing characteristics of hard facing overlays. Weld metal deposited by these electrodes shows far less porosity, greater freedom from cracking, a minimum amount of penetration, and much less dilution of the deposit by the base metal . . . test proven advantages that lengthen the life of the welded surface.

Since no one metal or alloy can satisfactorily meet the requirements of all hard surfacing problems, a complete line of HARDEX electrodes has been developed to give the best in wearing quality at the least cost per pound of metal deposited. Over a dozen different HARDEX electrodes offer hard surfacing materials ranging from 18 to over 60 Rockwell C. Alloyed for a wide range of applications, they perform exceptionally well under standard welding techniques using the electric arc and, in some cases, the oxy-acetylene method. Metal & Thermit's new catalog HS-56 fully describes each electrode, includes a Selector Chart to simplify selection of the right HARDEX electrode for over 200 applications. Your nearest Metal & Thermit representative can supply you with a copy.

Signs of progress in welding

During the past few months Metal & Thermit has introduced a number of new products for reducing costs in the welding field. Included are all-position iron powder electrodes such as Speedex R (E-6010), Speedex-HTS (proposed E-7018) for hard-to-weld steels, and Speedex-U (proposed E-6014) for high-speed welding; CROLOY electrodes for welding high temperature-high pressure equipment; KEL-RAY Projectors for radiographing welds; and the M&T Controlled Arc Power Supply for constant voltage semi- or full-automatic welding.

For the latest news on progress in welding keep an eye on M&T Welding Progress.

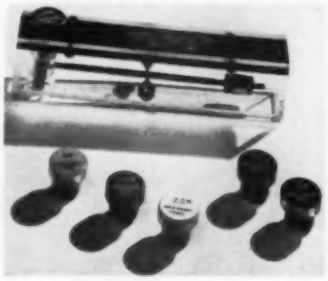
New E-6013 Electrode

A new improved E-6013 electrode, Murex U-13 assures dependable deposits in any welding position . . . on any type current. This general purpose electrode operates with a quiet, moderately penetrating arc, which is exceptionally easy to control.

Murex U-13 may be used for vertical down welding. In addition to vertical and overhead applications, it is ideal for single pass horizontal fillets and lap welds. Regardless of position, Murex U-13 deposits fast-freezing X-ray clean welds with high tensile strength, high impact and good ductility.

This new electrode operates at high amperages with light spatter. Dense slag, completely covering the deposit, is readily removed.

FERRITE INDICATOR



Ferrite Indicator is a simple, rugged, non-destructive inspection instrument developed for laboratory, shop, and field use. Go no-go feature permits instant acceptance testing of austenitic stainless and manganese weld and base metals. Requires no external standards, no adjustments, no visual readings, and is unaffected by position. Performs equally well on sheets, plates, rods, irregular shaped objects, and individual parts of complex components. Effective in locating areas of high and low ferrite content in heterogeneous products.

- Indicator covers all practical welding requirements; inserts being available in the range of 1.5% to 15% ferrite.
- Indicator weighs but three ounces and is 5" long. All metal components are bright nickel plated and the Indicator is furnished in a handsome hardwood box.

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unusual ease of operation in any working position. Stainless-steel stem and tube-within-a-tube construction retard heat conduction to the handle, assuring maximum operator comfort even after prolonged use. A needle valve at the rear of the 406 handle makes possible precise flame adjustment without overheating the soldering copper.

The 406 medium soldering iron is available with interchangeable pointed or chisel-edge copper soldering heads.

For complete details, write directly to Linde Air Products Co., 30 E. 42nd St., New York 17, N. Y.

Powered Turning Rolls

Unique Turntable and Equipment Corp. announces the availability of their model UTP 100T, 200,000-lb capacity, powered turning rolls. The power rolls and idlers are secured to carriages and placed on a large frame for easy adjustments.

This large type of push button power unit is a standard product with Unique Equipment, Inc. Unique power units and idlers in this capacity are built by using 8 rubber-tired wheels that are used for turning through a compound gearing arrangement to develop the high torque that is needed in an application of this type.

For complete information, write to Unique Turntable and Equipment Corp., 122 Sumpter St., Brooklyn 33, N. Y.

Single Eyepiece Goggle

A new single eyepiece goggle, designed and produced by the Fibre-Metal Products Co., is reported to improve the

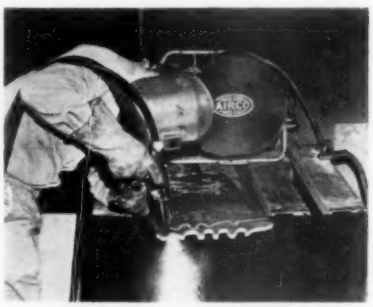
view of work being done with its "picture window" and to afford maximum safety and comfort to the worker. Trade marked "Solo goggle," it is designed to use standard 2 x 4 1/4-in. filter lenses and cover glass and to fit over even the largest, latest type safety or personal prescription glasses.

"Solo goggle" is available with head rest for pressureless close-to-face fit or for ventilated off-the-face wear.

Further information may be obtained by writing The Fibre-Metal Products Co., Chester, Pa.

Cutting Nonferrous Metals

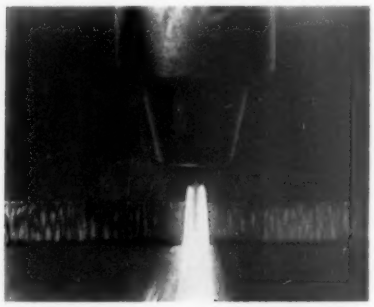
Air Reduction Sales Co. announces that the standard Aircomatic welding equipment can now be used for cutting nonferrous metals in addition to welding ferrous and nonferrous metals. This new process requires no special operator techniques. Simple adjustments in gas flow and steel wire speed are all that is needed. Both high-production machine equipment and manual



equipment can be adjusted for cutting with equal facility, the manufacturer states.

Aluminum, stainless steel, chrome steel, nickel, Monel, Inconel, copper, brass and aluminum bronze have been cut with a quality entirely satisfactory for subsequent welding operations. On 1/4-in. plate, cutting speeds up to 132 ipm can be attained on aluminum, and 85 ipm on stainless steel. Severence cuts have been made on titanium and magnesium.

Aircomatic welding, pioneered and developed by Air Reduction, employs a consumable wire electrode arc in an atmosphere of inert gas such as argon. The inert gas, introduced around the electrode wire, excludes all other atmospheric gases from the immediate area surrounding the electric arc and weld puddle. This prevents contamination of the weld by these other gases.



In welding, the arc is maintained between the tip or end of the wire and the upper surface of the plate; while in cutting, the arc is maintained between the leading edge of the wire and the leading edge of the cut extending throughout the full depth of the cut.

For complete details write to Air Reduction Sales Co., a division of Air Reduction Co., 150 E. 42nd St., New York 17, N. Y.

Monel Electrode

Fabricators of Monel metal fixtures and equipment will be interested in a new Monel welding electrode recently announced by the McKay Co., Pittsburgh, Pa. The electrode, called McKay Mon-Alloy, is reported to insure crack-free, porosity-free weld deposits under an extremely wide range of welding conditions. They conform to the requirements of Bureau of Ships Specification MIL-E-17496A and AWS-ASTM Specification A5.11-54T.

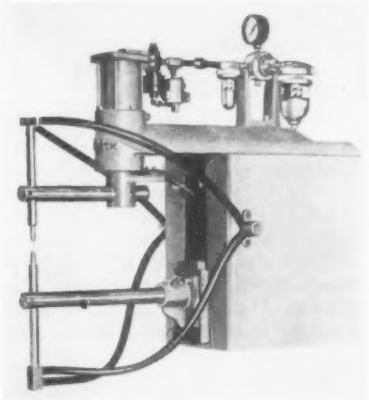
Mon-Alloy electrodes are used with d-c reverse polarity current and employ a modified titania-type coating. According to manufacturer, arc action is smooth and quiet with minimum spatter loss. A specially balanced molten slag provides good fluxing and wetting action when welding either carbon steel or

Monel base material.

A technical bulletin giving full specifications, composition and application data is available from the McKay Co., 1005 Liberty Ave., Pittsburgh 22, Pa.

Bench-Type Welders

Two new bench-type, air-operated 5- and 10-kva spot or projection welders, specially designed for light and nonferrous metals such as are used in the appliance and electronic tube field, have



been added to their line by Weldex, Inc., 18390 Weaver St., Detroit 28, Mich.

According to manufacturer, features include valve and speed controls mounted at the cylinder for easy access and greater speed, and a ram bearing surface of 15 sq in. at full stroke.

For complete details, write directly to the company at the above address.

NEW LITERATURE

Special Copper Alloys

A tabulation of nonstandard, wrought copper base alloys has been prepared by the Technical Committee, Copper & Brass Research Assn. It is the first compilation of special copper alloys—those not listed in the Association's Manual of Standards—ever made by the brass mill industry.

The list provides the trade name, generic name, nominal composition, forms available, typical applications and mill source for each alloy. Requests for free copies should be made to Carl Pihl, Secretary, Copper & Brass Research Assn., 420 Lexington Ave., New York 17, N. Y.

Argon-Arc Welding of Aluminum

The British Welding Research Assn., 29 Park Crescent, London, W. 1, has recently released the first two of a series of booklets on the argon-arc welding of aluminum and aluminum alloys.

The first booklet, entitled "The Principles of Argon-Arc Welding Aluminium," contains 18 pages and is priced at 2*s*.6*d*. The second booklet, "Electrical Characteristics and Equipment for Argon-Arc Welding Aluminium," is largely a summary of research on electrical aspects of argon-arc welding carried out in the laboratories of the Electrical Research Assn., in cooperation with the BWRA, during the years 1949–51. It contains 20 pages and its price is 5*s*.

Those interested in obtaining copies of these booklets should write directly to the BWRA at the above address.

Historical Booklet

"The Fight Against Fahrenheit," a new two-color, 12-page booklet (TIS 2644) describing the importance of the United States Patent System to its research and development of advanced metal-joining alloys and processes is available from Technical Information Service, Eutectic Welding Alloys Corp., 40-40 172nd St., Flushing 58, N. Y.

The booklet traces the history of welding from the 13th Century forge- and water-operated bellows or "leather lungs" used by the blacksmith. Interesting information is given on the bonfires used to preheat before introduction of the oxygen-acetylene torch at the turn of the 19th Century. Interesting historical drawings contrast with photomicrographs made during modern metallurgical research to show the advance of welding.

Main Steam Piping

Complimentary copies of the paper, "Metallurgical Considerations of Main Steam Piping for High-Temperature, High-Pressure Service," by H. S. Blumberg, are available from the Tempil® Corp., 132 West 22 St., New York 11, N.Y.

Great economic significance attaches to this paper in view of the present trend to increase temperatures and pressures in large steam generating installations. This is said to be the first comprehensive review and discussion of technical literature on new superstrength alloys for main steam piping.

Reprints of this report (which was presented at the ASME annual meeting in New York on Nov. 28, 1956) are also obtainable from the ASME at 25¢ to members and 50¢ to nonmembers.

Abrasive Selection Guide

Pangborn Corp., Hagerstown, Md., manufacturers of blast cleaning and dust control equipment has just issued a new 12-page illustrated manual on the full spectrum of blast cleaning abrasives—their characteristics, application and selection. The work details methods for improving cleaning quality and cutting production costs in foundry descaling, mold and die, shop maintenance, decorating and carving, part reclamation and finishing operations.

Copies of Bulletin No. 333 may be obtained from the Pangborn Corp., Hagerstown, Md.

Stud Welding

A complete 36-page booklet on industry Standards in arc welding studs, pins and tapped pads for stud welding has been published by KSM Products, Inc., Stud Welding Division, Merchantville 8, N. J.

The booklet is concise and easy to read. It contains a complete index and reference to equipment and accessories used in stud welding applications.

Each specification page lists size minimums and maximums. It details mechanical properties, chemistry, thread sizes, plating and annealing information and other data valuable to engineers, production and purchasing men.

The booklet may be obtained free of charge by writing to the manufacturer at the above address.

Ampco Welding News

The Fourth Quarter Issue of the *Ampco Welding News*, published by the Weldrod Department of Ampco Metal, Inc., was recently released and contains interesting articles pertaining to welding, safety tools and centrifugal pumps.

The front page feature article gives a detailed description of the repair of a cast-steel, Francis-type turbine runner in Eitting, Austria, with Ampco-Trode 160 electrodes.

Free copies of this publication may be obtained by writing to Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wis.

Arc-Cutting Torch

Arcair Co., 419 S. Mt. Pleasant St., Lancaster, Ohio, announces the publication of a new 2-color, 4-page folder on the Arcair process and the use of the Arcair torch for the cutting, gouging, beveling or grooving of all metals. This folder features numerous new applications for the Arcair torch, and also includes data on Arcair electrodes.

Free copies are available to interested parties by writing directly to the company at the above address.

Here's Just 3 Reasons Why So Many Welders Have Switched

- Wrap around Glass Fibre Tip Insulation—30% *more* heat resistance than any other make!
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Steel Mill Maintenance

A six-page reprint entitled "Welding and Cutting in Steel Mill Maintenance" by R. L. Deily, steel mill representative, Air Reduction, is now available.

Reprinted from *Iron and Steel Engineer*, this paper deals with mechanization in steel mill mechanical maintenance. There are discussions on mechanized oxygen cutting, electric welding, including inert-gas-shielded processes, and the new iron-powder electrodes.

You can obtain your copy by writing to Air Reduction Sales Co., a division of Air Reduction Co., Inc., 150 E. 42nd St., New York 17, N. Y. Specify form ADR 109.

RWMA Technical Bulletins Available

The Welding Practices Sub-Committee of the Resistance Welder Manufacturers' Assn., with the permission of THE WELDING JOURNAL, has recently published several articles on Reistance Welding in bulletin form.

Spot Welding of Magnesium with Three-Phase Low-Frequency Equipment has been designated Bulletin No. 22 of the RWMA series.

Bulletin No. 23 is *Seam Welding Low-Carbon Steel* and *Seam Welding Dissimilar Thickness of Low-Carbon Steel.*

Both of these, together with the many other bulletins, and the first volume of the Resistance Welding Manual are available at nominal charges, at the office of the Secretary, 1900 Arch St., Philadelphia 3, Pa.

Aluminum Designation Chart

Any user of light metals will be sent from All-State Welding Alloys Co., Inc., 249–55 Ferris Ave., White Plains, N. Y., as many copies as he may need of a chart to convert old aluminum-type designations to the new designations now in use. The conversion chart is on a single punched sheet, $8^1/_2$ x 11 in.

Included on the same sheet with the conversion chart are six other charts concerning aluminum. They are especially designed to aid in the wise selection of alloys and fluxes to weld, braze and solder aluminum . . . are without advertising.

Requests may be addressed to the company—ask any All-State distributor for a copy of the aluminum chart.

Reinforced Abrasive Products

A new catalog of reinforced resinoid abrasive products, which have become an essential part of modern foundry, welding and metal fabrication industries, is being offered by the Bay State Abrasive Products Co., Westboro, Mass.

The index is arranged so that information on the right type of reinforced resinoid wheel may be easily located by picking out the type of operation involved or material to be cut.

For your copy, write directly to the company at the above address.

Metal Joining Techniques

"Techniques of Metals Joining," a new series of technical articles published by the Technical Information Service, Eutectic Welding Alloys Corp., 40–40 172nd St., Flushing 58, N. Y., gets under way with a nine-page fully illustrated reprint of "Production Brazing" (TIS 2635), a paper delivered to the 1956 meeting of the International Institute of Welding, at Madrid, Spain.

The paper was prepared and delivered by Dr. G. M. A. Blanc, Research Engineer for Societé des Soudures Castolin S.A., Eutectic's European affiliate company.

Copies of "Production Brazing" are available at no cost by writing to "Eutectic's" Technical Information Service and asking for TIS 2635 or from any Eutectic District Engineer.

Alloys and Fluxes for joining and sealing

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- Composition of aluminum alloys
- Government specifications and aluminum types that meet them
- Where and how to use the different aluminum types
- Solders for aluminum to aluminum and aluminum to dissimilar metals . . . melt and flow temperatures, plastic range, strength, available forms and applicability to various production processes

ASK YOUR ALL-STATE DISTRIBUTOR FOR COPIES OF THE ALUMINUM CHART

ALL-STATE WELDING ALLOYS CO., INC., WHITE PLAINS, N. Y.

Resistance Welding Electrodes

The complete line of Ampco-Weld resistance welding electrodes and accessories is described in Bulletin RW-10, released by Ampco Metal, Inc., Milwaukee, Wis.

Included in the 28-page, two-color catalog are applications, specifications and dimensions of standard welding tips, wheels, dies and holders. Also offered is descriptive and highly illustrative data on holder-to-tip adapters, quick disconnect shutoff couplings, seam welder shafts and bushings.

Bulletin RW-10 is available at no cost from Ampco Metal, Inc., 1745 S. 38th St., Milwaukee 46, Wisc., or from any Ampco resistance welding distributor.

Welding News

An "Operation Greenland" special issue of *Eutectic Welding News* (TIS 2611) describing Diesel cylinder repair procedures developed for Thule Air Base by Eutectic's Service Supervisors is available from Technical Information Service, Eutectic Welding Alloys Corp., 40-40 172nd St., Flushing 58, N. Y.

This issue of *Eutectic Welding News* describes the repairing of equipment subjected to −30 to −40 deg temperatures and arctic winds in excess of 100 mph.

Copies of TIS 2611, *Eutectic Welding News* "Operation Greenland" are also available from more than 350 Eutectic District Engineers.

Solo Goggle

A two-page, three-color bulletin, issued by the Fibre-Metal Products Co., describes the company's new "Solo goggle," a single unit "picture window" goggle for full view and maximum protection for the eyes in brazing, cutting, gas welding, grinding and similar work.

The Solo goggle is available with an adjustable elastic headband or with a headrest support with a positive single point friction joint (which may also be used with safety caps).

This Bulletin No. 53 may be obtained by writing the Fibre-Metal Products Co., Chester, Pa.

Ultrasonic Welding

The availability of a new four-page "Sonobraze" leaflet is announced by Aeroprojects, Inc., 310 E. Rosedale Ave., West Chester, Pa. "Sonobraze" is ultrasonic equipment for fluxless brazing and for coating of aluminum. "Sonobraze" is particularly suited to the joining of unstripped enameled aluminum wire.

Free copies of this brochure are available upon request to Aeroprojects, Inc., at above address.

Temperature Conversion Chart

The Moeller Instrument Co. is offering without obligation its temperature conversion chart to engineers, students and others having use for this handy pocket size aid.

The easy-to-read tables of Fahrenheit and centigrade temperature equivalents are intended as a time saver to the busy man having frequent occasion to convert from one temperature to the other.

To obtain one of these handy charts, readers should write to Moeller Instrument Co., Richmond Hill 18, N. Y.

Ceramics for Jigs

Bulletin 107 describing Sur-Braze Grade M120-F ceramic for electrical and chemical insulators, brazing and heat-treating jigs for use at 2000° F is now available at no charge from Technion Design & Manufacturing Co., Inc., 262–72 Mott St., New York 12, N. Y.

Bulletin 107 gives complete thermal and mechanical properties of this new ceramic. M120-F is available in raw form which is easily machined and fired by the customers or in precision-machined shapes fabricated by Technion to print. M120-F is said to be noncarbonizing, noncorrosive and resistant to acids, alkalies and oils.

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Welding Research

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W. SPRARAGEN, DIRECTOR

SUPPLEMENT TO THE WELDING JOURNAL, FEBRUARY, 1957

FURTHER STUDIES ON STAINLESS-STEEL HOT CRACKING

Test data provides additional evidence supporting the hypothesis that grain boundary liquation is responsible for base-metal hot cracking of stainless steels

BY P. P. PUZAK AND H. RISCHALL

ABSTRACT. Previous studies of several Types 347 and 304 stainless steels suggested that the development of weld- and base-metal hot cracking involved the grain boundary liquation of fusible segregates. The results of additional studies are presented for these materials with special attention directed toward comparing test results for a Type 347 heat (which was established previously to be most susceptible to hot cracking) with those for a standard Type 304 heat (which was not susceptible). Liquidus and solidus temperatures for all materials were obtained by thermal analysis techniques. The hot ductility and strength developed in high-temperature tensile tests conducted in the temperature range below the region of normal incipient melting were determined to be of low order only for those heats which exhibited base-metal hot cracking. Thus, the test data provide further evidence supporting the hypothesis that grain boundary liquation is responsible for base-metal hot cracking of stainless steels.

The results of microscopic examinations, and X-ray studies for the identification of the liquation constituents are presented and discussed.

Introduction

In the welding of heavy sections of the columbium bearing, Type 347 stainless steels, sporadic base-metal hot cracking, as well as weld-metal hot cracking has generally been encountered. For the most part, the weld-metal hot cracking problem has been alleviated by the generally accepted practice of specifying the use of weld metal compositions which are adjusted so as to form specific percentages of ferrite in the microstructure of the weld deposit. Such practices, however, have never proved successful in minimizing the base-metal hot cracking problem. A further complication is observed in fabrication and/or service, in that the sporadic base-metal cracking is developed in some heats of Type 347 material but not in others even though both apparently have been made to the same specifications. In addition, for those materials for which no base-metal hot cracking is observed by conventional inspection procedures after welding, considerable cracking has been experienced following heat treatment and/or elevated temperature service of these weldments. A rational explanation of such behavior, requires a fundamental solution as to the precise mechanism of crack formation and/or propagation developed in the welding, heat treatment and service of the Type 347 alloys.

Previous investigations of stainless steels conducted at the Naval Research Laboratory have suggested that the presence of low-melting, grain-boundary films are responsible for the hot cracking developed immediately after welding.[1, 2] Test results were also presented which demonstrated the possibilities of a stress-rupture type propagation at elevated temperatures, of low-ductility fractures originating at minute crack flaws which are developed by the liquation of grain boundaries during welding of the Type 347 alloys. The investigation described in this paper was aimed at developing information concerning the actual presence of liquation films by conducting critical experiments of hot ductility and strength above and below the solidus (bulk) temperatures of those stainless steels determined previously to be susceptible or not susceptible to hot cracking (Table 1).

Thermal Analysis Determinations

Carefully instrumented thermal heating and cooling curves were obtained for the various 347 and 304 materials. The samples were heated under an argon atmosphere to their molten state by means of an insulated glo-bar furnace. A program controller and two calibrated Pt +13% Pt-Rh thermocouple recorders were used to indicate, respectively, the true metal and furnace (controlled cooling rate 5° F/hr) temperatures. From conventional time-temperature and inverse rate cooling plots, the "bulk" solidus and liquidus temperatures were obtained. For purposes of this investigation, these determinations are shown for each material (Table 2) approximated to the closest 5° F integer below the thermal arrest of the inverse rate plot. The nominal melting ranges indicated in the literature[3] for the Type 347 and 304 alloys are also shown

P. P. Puzak is Head, Welding Section, Metal Processing Branch, U. S. Naval Research Laboratory, Washington, D. C. **H. Rischall** is with Utica Drop Forge & Tool Co.; formerly Metallurgist, U. S. Naval Research Laboratory, Washington, D. C.

Presented at the 1956 AWS National Fall Meeting in Cleveland, Ohio, October 8–12.

Table 1—Heat Number and Type

	53313, low-C 347, high Cb*	*53316,* low-C 347, low Cb*	*53320,* Std. 347*	*53330,* Std. 304*	*53327,* low-C 304*
C, %	0.03	0.04	0.09	0.09	0.03
Mn, %	1.44	1.40	1.46	1.01	1.07
Si, %	0.74	0.52	0.53	0.52	0.50
S, %	0.014	0.017	0.012	0.024	0.013
P, %	0.025	0.022	0.021	0.027	0.031
Cr, %	17.9	18.1	18.2	18.8	18.6
Ni, %	13.5	13.9	10.6	9.7	10.4
Mo, %	0.21	0.22	0.26	0.30	0.43
Cb + Ta, %	0.98	0.50	1.15	0.12	0.12
Cracking susceptibility†	Most severe	Severe	Borderline	None	None

* The last two digits of the heat numbers are used as code numbers to represent the above materials.

† 2 to 1 reduction from original ingot.

for comparison purposes. It is seen that the test determinations of liquidus and solidus temperatures are in good agreement with the nominal melting ranges for these materials indicated in the literature.

Short-Time High-Temperature Tensile Tests

Standard 0.505-in. tensile specimens were machined from each of the plate materials (2 to 1 reduction). Elevated-temperature tensile tests were conducted at various intervals of 50 or 100° F between the temperature range of 2100 to 2600° F. The apparatus employed consisted of a glo-bar furnace, control equipment and argon atmosphere. From initial investigations, it was established that accurate test temperatures were obtained by the use of a thermocouple maintained in contact with the central surface area of the tensile specimen. Specimen oxidation was minimized by rapidly heating until the desired temperature was indicated by the thermocouple contacting the specimen surface and controlling at this temperature for a two-minute holding time before applying the tensile load.

Figure 1 presents the results of these tests for the standard Type 304 material. For this material it is observed that relatively high ductility and strength are maintained at all temperatures of testing except that (2550° F) immediately above the bulk solidus temperature (2540° F) previously recorded for this alloy. Figure 2 illustrates test results obtained for the low-C, high-Cb Type 347 material. A significant difference is evident in the performance of this material from that obtained with standard Type 304 tests. Whereas square, nil-ductile fractures were obtained with extremely low loads (10 to 15 lb) for the Type 304 material only at tests conducted above its solidus temperature, similar square fractures were obtained at equally low loads for the low-C, high-Cb Type 347 tests at

Table 2—Thermal Analysis Results

Code	*Type*	*Literature[3] values for the melting range, ° F*	*Measurements* — *Solidus temperature, ° F*	*Liquidus temperature, ° F*
13	Low-C 347 (high Cb)	...	2565	2605
16	Low-C 347 (low Cb)	...	2565	2620
20	Std. 347	(2550–2600)	2550	2610
30	Std. 304	(2550–2650)	2540	2650
27	Low-C 304	(2550–2650)	2580	2650

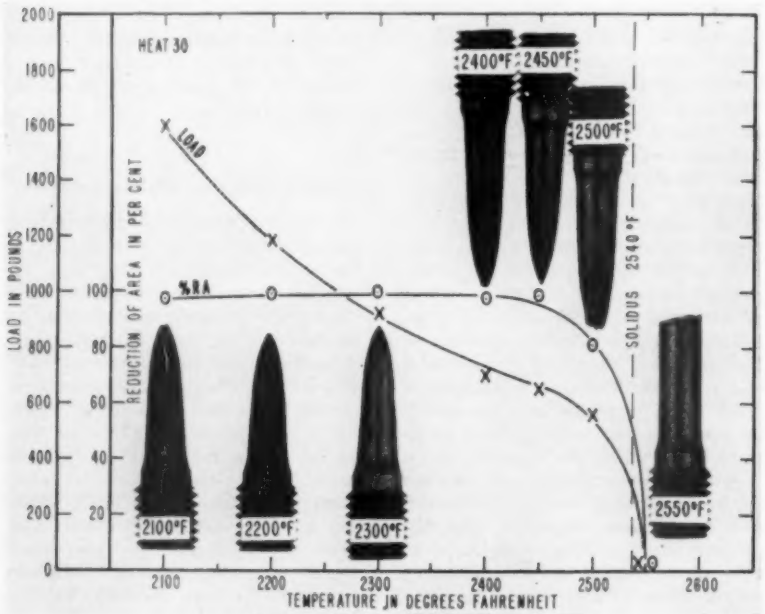

Fig. 1 Elevated temperature test results for standard Type 304 alloy. Note retention of hot ductility and strength characteristics at all temperatures until solidus temperature is exceeded which is indicative of a lack of fusible grain boundary segregates

temperatures (2450° F) approximately 100° F below its bulk solidus temperature (2565° F).

The performance exhibited above by the standard Type 304 and the low-C, high-Cb Type 347 materials was readily duplicated in additional tests conducted over the same temperature range. For the balance of the materials studied previously (low-C, low-Cb Type 347, standard Type 347, and low-C Type 304) similar tests were conducted on a limited basis to establish that temperature at which these materials displayed low-load, low-ductility ruptures which could be ascribed to the liquation of grain boundary areas. Test results for the five materials investigated are summarized in Table 3.

For the low-C, high-Cb Type 347 material, additional hot tensile tests were conducted with the portions of this heat which had received a higher degree of forging reduction from the ingot (5 to 2 and 10 to 2). The results of these tests, in comparison with former results given for the 2 to 1 reduction material, are presented in Figs. 3 and 4. It is observed that the higher degree of working has improved considerably the hot-ductility and hot-strength character-

Table 3—Results of High-Temperature Tensile Tests

Code	Type	*NRL solidus temperature, ° F*	*Liquation rupturing temperature, ° F**
13	Low-C 347 (high Cb)	2565	2450
16	Low-C 347 (low Cb)	2565	2500
20	Std. 347	2550	2500
30	Std. 304	2540	2550
27	Low-C 304	2580	2600

* Temperature at which nil-ductile ruptures developed with tensile loads of 10 to 15 lb.

istics at 2400° F but not at 2450° F.

The fracture surfaces exhibited by all specimens of these materials which developed low-ductility, low-load fractures were identical in appearance. They were typically intergranular and dendritic fractures, characteristic of ruptures by grain boundary paths, as represented by the photograph (Fig. 5) of the low-C, high-Cb Type 347 specimen broken at 2450° F (approximately 100° F below its bulk solidus temperature). Metallographic studies were conducted for sections removed from the various standard Type 304 and the low-C, high-Cb Type 347 tensile specimens. The presence of grain boundary liquation areas is clearly visible in the latter material for the sample broken at a temperature of 2450° F (Fig. 6). As would be expected, similar areas were observed for the standard Type 304 samples which were broken at 2550° F (10° F above their solidus temperature). However, no evidences of such liquation could be detected in any of the other specimens (standard 304 or low-C, high-Cb 347) which were tested at temperatures lower than those discussed above.

In the former investigation,[2] weldability tests evaluated the cracking propensities of these materials as being either highly susceptible or not susceptible to hot cracking as shown in Table 1. Test results presented herein are seen to be consistent with previous deductions concerning the development of fusible grain boundary films in these Type 347 but not in the Type 304 alloys investigated herein. Both of the Type 304 alloys have been shown to lose their hot ductility and strength characteristics only at temperatures immediately above their solidus temperatures. Thus, lacking fusible, grain boundary constituents, these particular Type 304 alloys are not prone to hot cracking by a grain boundary liquation mechanism at temperatures below the fusion line temperature (temperatures in the heat-affected zone).

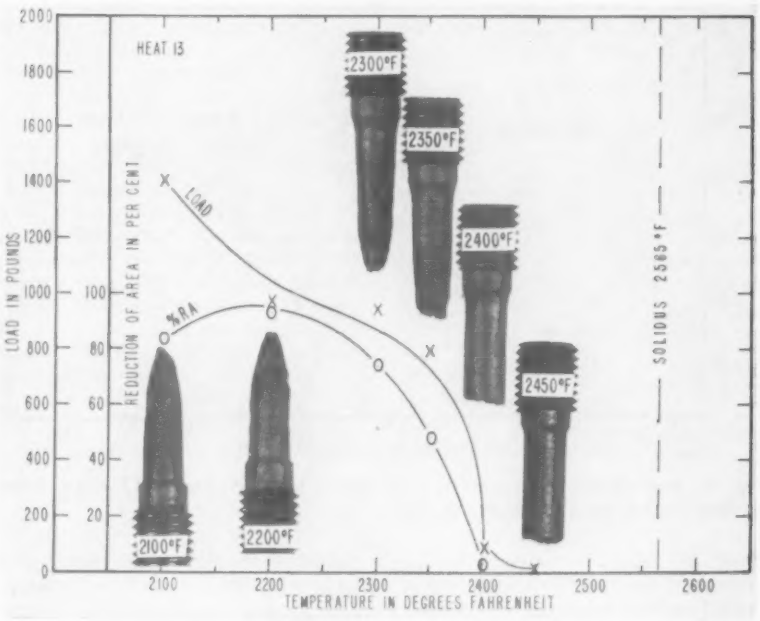

Fig. 2 Elevated temperature tensile test results for low-C, high-Cb Type 347 alloy. Note loss of hot ductility and strength characteristics at temperatures immediately below bulk solidus temperature which is indicative of liquation of fusible grain boundary segregates

The Type 347 alloys studied herein, however, have been shown to exhibit an abrupt loss of hot ductility and strength at temperatures below their respective bulk solidus temperatures and, therefore, are susceptible to grain boundary hot cracking in the heat-affected area of the weld. Microscopic evidences of liquation areas in the low-C, high-Cb alloy developed at approximately 100° F below its bulk solidus temperature can be attributed only to the presence of fusible grain boundary films.

It is generally accepted that increased forging reductions from the ingot tend to improve cracking resistance of materials which are observed to be moderately susceptible to hot cracking at low forging reductions. For the low-C, high-Cb

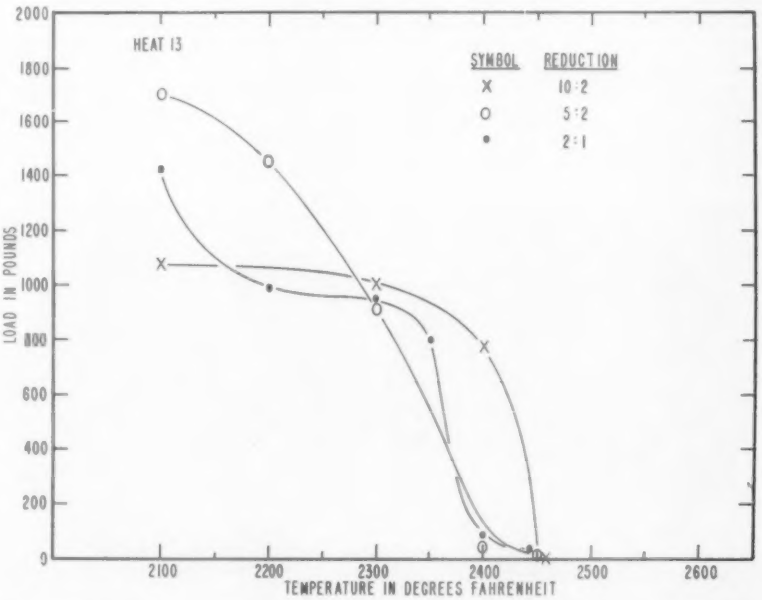

Fig. 3 Hot strength characteristics of low-C, high-Cb Type 347 alloy after various forging reductions from the ingot

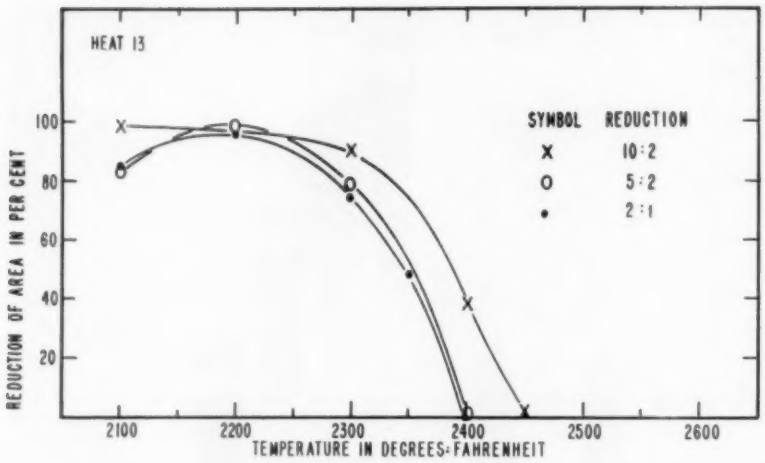

Fig. 4 Hot ductility characteristics of low-C, high-Cb Type 347 alloy after various forging reductions from the ingot

Type 347 alloy investigated herein, however, it is seen that increasing forging reductions have not changed the "liquation-rupturing" temperature (Figs. 3 and 4) for this material. It is deduced that the additional forging, to the extent indicated, has not resulted in a complete and effective redistribution of the fusible segregate in this particular Type 347 alloy in that a finite liquation temperature range was retained. The hot tensile data presented herein are, nevertheless, consistent with previous weldability test results, in that a greater number of cracks, of considerably smaller size, were developed in this material in the highly reforged condition than that obtained for the 2 to 1 reduction material. It should be noted that this material does not conform to the nominal composition range of the Type 347 alloys because of the unusually high nickel content (13.5%) as well as the intentionally high Cb to C ratio (approximately 30 to 1). For the standard (nominal) Type 347 composition, forging reductions to the extent studied herein are adequate to improve crack resistance, and this was so demonstrated for these materials in previous studies.

Nature of Fusible Segregate Composition

The test results presented above indicate that the low-C, high-Cb Type 347 alloy contained a low-melting point segregate composition concentrated at the grain boundaries in an amount which might be amenable to quantitative identification. The standard 304 alloy, on the other hand, was considered to be completely lacking of any segregate compositions which remained fusible at temperatures below that of the bulk solidus. These alloys were selected for tests aimed at identification of the segregate composition deduced to be present in the Type 347 alloy.

Samples from both materials were examined by spectrochemical techniques. Both steels appeared to be exceptionally "clean" in that no evidence could be detected of any of the "tramp-elements" (Al, Sn, Pb, B, etc.) sometimes associated with these compositions as the result of their possible retention from the scrap charges. In addition, an emission spectrochemical analysis was conducted for essentially the grain boundary material (insoluble residue) obtained from the low-C, high-Cb Type 347 alloy after a selective plating procedure was employed to dissolve the austenitic matrix in a copper-potassium-chloride solution ($CuCl_2 \cdot KCl$).[4] Test results indicated the residue to be predominantly Cb with minor amounts of Mn, Si and Fe.

As-received samples of both materials were subjected to X-ray diffraction studies. The diffraction patterns obtained were interpreted for the Type 304 alloy to be that of a 100% austenitic matrix. For the Type 347 alloy, however, the heavy austenitic-matrix pattern was combined with a pattern identified by these techniques to be that associated with columbium carbide (CbC) and/or columbium nitride (CbN), presumably together in solid solution as (CbC, CbN) such as that identified in the work of Dulis and Smith.[5]

The results of nitrogen determinations made for the two materials are as follows:

Standard 304		*Low-C, high-Cb 347*
0.02%	Soluble portion	0.004%
...	Insoluble portion	0.013%
0.02%	Total	0.017%

Whereas the total nitrogen contents for the two materials are approximately the same, the percentage of insoluble nitrogen present in the Type 347 material supports the occurrence of a nitride in combination with the carbide observed in the X-ray diffraction studies of the low-C, high-Cb Type 347 material.

Summary and Conclusions

The elevated temperature tensile

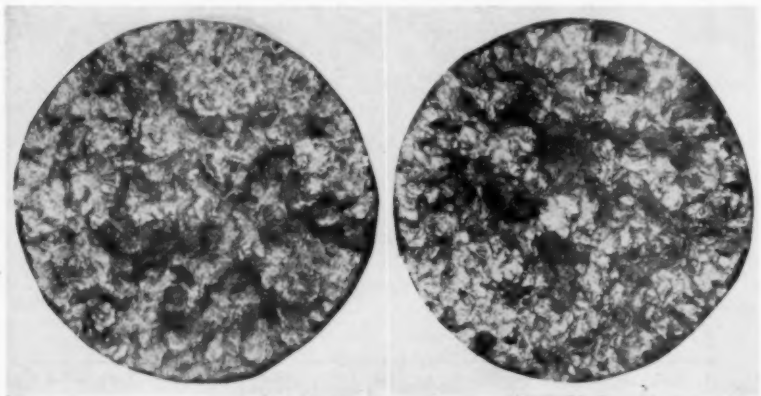

Fig. 5 Fracture surfaces of low-C, high-Cb Type 347 sample ruptured at 2450° F (approximately 100° F below solidus temperature) with low loads (10–15 lb)

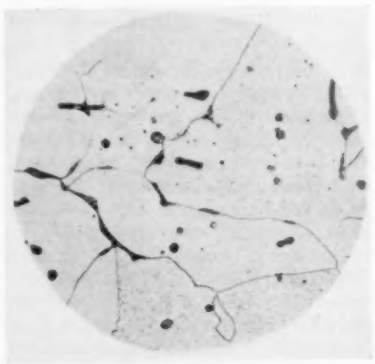

Fig. 6 Photomicrograph illustrating grain boundary liquation areas developed in low-C, high-Cb Type 347 tensile specimen ruptured at 2450° F. Magnification X 100. Etchant: 75% marbles reagent + 25% H_2O. Reduced by one half upon reproduction

tests demonstrate conclusively that low strength and ductility are developed at temperatures below their bulk solidus temperatures for stainless steels determined previously to be of cracking types but not for those of noncracking types. Results of these tests and of subsequent metallographic studies, corroborate previous deductions concerning the cracking propensities of the Type 347 alloys investigated herein as resulting from the liquation of fusible grain boundary segregate materials. These data conform to the liquation hypothesis evolved in previous investigations[6, 7] concerning the mechanism of the development of hot cracking (tearing) defects in castings. Additional casting studies[8] have demonstrated that the abrupt nature of the strength and ductility changes which occur at hot cracking temperatures is characteristic of this liquation process for all materials at their bulk solidus temperature and also at a "depressed-solidus" temperature for those materials containing low-melting, grain boundary segregates (e.g., sulfides, etc.).

Chemical analysis and X-ray diffraction studies suggest the presence of a eutectic-type mixture of carbides and nitrides of columbium with minor amounts of Mn, Si and Fe as representing the probable segregate composition involved in liquation cracking of the Type 347 alloys.

Acknowledgment

The authors acknowledge the support and interest in this investigation by T. Griffin of Code 537, Bureau of Ships.

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RESEARCH NEWS

X-ray Microscope

A powerful new tool for research into hitherto unexplored fields of metals has been announced by the General Electric Co., X-Ray Department, Milwaukee 1, Wis.

Under development for more than 4 years, the X-ray microscope promises to reveal hidden facts concerning such vital facts as corrosion in metals, the effect of foreign materials in metals, the soundness of electroplated coatings and the effects of various processes on the behavior and structure of metals.

Secret of the X-ray microscope lies in the extremely tiny size of the focal spot, which is bombarded by electrons and thus caused to give off X-rays. This target is only 40 millionths of an inch in diameter. X-rays emanating from such a small spot fan out in all directions and hence magnify any object which they pass through. The X-ray microscope achieves magnifications up to 1500 times, and at the same time discloses the inner structure of the object being examined.

While not as powerful in its magnifying power as the electron microscope, the X-ray microscope has an advantage in that it does not require mounting the specimen in a vacuum and other special preparation techniques.

It has an advantage over the optical microscope in depth of focus which permits three-dimensional examination of the specimen. No special sectioning is required to prepare the specimen.

Its inherent advantage over both is that it penetrates to the interior of the object and reveals structures otherwise not visible.

The X-ray microscope may be used either for direct viewing or for recording the image on film.

X-ray Spectrograph Chart

A new, revised $17^1/_2$ x $22^1/_2$" X-ray spectrograph chart showing characteristic secondary X-ray beams for elements from magnesium (atomic No. 12) to californium (atomic No. 98), is available gratis from the Instruments Division, North American Philips Co., Inc., 750 S. Fulton Ave., Mount Vernon, N. Y.

The new chart is suitable for wall display and is intended to assist technicians and other personnel to understand the theory and application of X-ray spectrography. It will also be useful in handling actual problems.

K-Alpha and K-Beta lines are shown for the following analyzing crystals: EDDT (ethylene diamine *d*-tartrate), ADP (ammonium dihydrogen phosphate), topaz, lithium fluoride and rock salt. L-Alpha$_1$ and L-Beta$_1$ lines, 1st and 2nd orders, are also shown for rock salt analyzing crystal.

Horizontal scales are provided on the chart to show two Theta angles in degrees and wavelengths in Angstroms for all elements under various operating conditions.

WELDING OF HIGH-STRENGTH PRESSURE-VESSEL STEELS IN HEAVY SECTIONS

Welding procedure qualification tests developed for 4-in. thick plates in accordance with ASME Code requirements

BY A. P. BUNK

SUMMARY. One carbon steel and three low-alloy steels in the 4-in. thickness were welded under varying conditions of preheat and postwelding heat treatment to determine their adequacy for meeting ASME Code requirements. The steels investigated were normalized A201A, normalized plus tempered A302B, normalized plus tempered Ni-Cu-Mo-V steel and a quenched plus tempered Mn-Ni-Cr-Cu Mo-V steel. Results from the 4-in. low-alloy steel plates are compared to those obtained with 1-in. thickness.

A201A in the 4-in. thickness met all the requirements when welded with E7016 electrodes and subsequently stress relieved.

While the A302B steel in the 1-in. thickness was successfully welded (E7016) with and without preheat provided a single or multiple stress-relief treatment was employed, the performance in the 4-in. thickness indicated the necessity for a 300° F preheat to avoid cracking. In addition, the E7016 electrode demonstrated insufficient strength when used on 4-in. material. E9015 welds were satisfactory when preheated and stress relieved.

Tests of Ni-Cu-Mo-V steel welded with no preheat, and with a 300° F preheat using E10016 (Ni-Mo-V) electrodes, without stress relief were successful for both thicknesses. Similarly, successful results were obtained with E9015 electrodes when no preheat was used and single or quadruple stress-relief cycles were employed. In addition, the test indicated a much higher plate strength for the 4-in. material than for the 1-in. stock.

For 1-in. and 4-in. plates, the Mn-Ni-Cr-Cu-Mo-V steel met all requirements when welded with E12015 (Ni-Mo-V) electrodes, no preheat, and no stress relief. Additional tests made with no preheat but with one or four cycles of stress relief were satisfactory in the 1-in. thickness when E9015 electrodes were employed. A slight tendency toward cracking was evident in the heavier thickness when welded without preheat. Crack-free welds made under no preheat conditions employing E9015 electrodes showed adequate strength and ductility with four cycles of stress relief. The strength level of the 4-in. material was considerably below the level obtained with 1-in. plate.

These welded steels have indicated good mechanical properties in both the 1- and 4-in. thicknesses. The encouraging results obtained without preheat and/or without stress relief in the greater thickness must be viewed with extreme caution since small-scale tests are not always directly applicable to full sized structures. As a consequence, it may be necessary, under certain conditions, to employ preheat on a material that normally requires no preheat. Since no hard and fast rule is available for predicting the degree and extent of preheat, reliance upon experience will largely determine the application of preheat.

FOREWORD. The investigation reported herein was conducted at the Chicago Bridge and Iron Co. for the Welding Research Council's Weldability Committee and Pressure Vessel Research Committee. The author is grateful to members of both committees for their counsel.

Introduction

The Materials Division of the Pressure Vessel Research Committee had previously investigated the mechanical properties of six high-strength steels in the 1-in. thickness.[1,2] Simultaneously, the Weldability Committee of the Welding Research Council sponsored weldability tests on the same steels. One phase of the welding tests involved procedure qualifications to determine the adequacy of existing procedures in meeting ASME requirements.[3]

Subsequently, at a joint meeting of the cooperating groups, it was revealed that with the tendency for increased operating pressure in many vessel applications, shell thicknesses of 5 and 6 in. were not uncommon. It was further revealed that while many carbon and low-alloy steels had been investigated in thicknesses around 1 in., very little published information was available for carbon steels and even less for low-alloy steels, in greater thicknesses. As a consequence, it was decided to extend both the mechanical property and welding tests to the 4 in. thickness. The purpose of this investigation was to determine the adequacy of welding procedures in meeting ASME Code requirements when 4-in. thick carbon and low-alloy steels are joined by the manual metal-arc process.

Description of Tests

Test welds consisted of joining two plates 12 x 4 x 24 in. along the 24-in. edge—the 24-in. edge having been previously machined to produce the joint geometry shown in Fig. 1. Each plate of the assembly was machined so the 24-in. dimension was parallel to the rolling direction of the plate. The two plates were then end restrained, as shown in Fig. 2, after a deliberate $^1/_8$-in. versine was introduced into the assembly. Initial tests, wherein only short run-off tabs were employed, resulted in severe distortion after only two or three passes were deposited on the first side. Subsequent back gouging and welding of the second side in an attempt to keep the plates in a common plane resulted in very severe cracking at the juncture of the weld bead and bevel wall. The use of end plates not only eliminated cracking, but permitted the entire first side to be welded before the assembly had to be turned for arc gouging and welding of the second side.

In connection with joint preparation, the selection of a double "U" for a geometry was based on minimizing the amount of metal deposited and keeping the plates in a common plane. The difficulties encountered in machining test coupons from an assembly are reduced markedly if the plates are kept flat. It is fully realized, however, that in actual fabrication, joint geometries are used which differ considerably from the one employed here. The reasons for the difference may be found in economy, accessibility or convenience.

For those assemblies which were not preheated initially, an interpass temperature of 120° F maximum was

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The tests reported herein were conducted at the Chicago Bridge & Iron Co. for the Subcommittee on High Strength Steels, Materials Division, Pressure Vessel Research Committee and the Weldability Committee of the Welding Research Council.

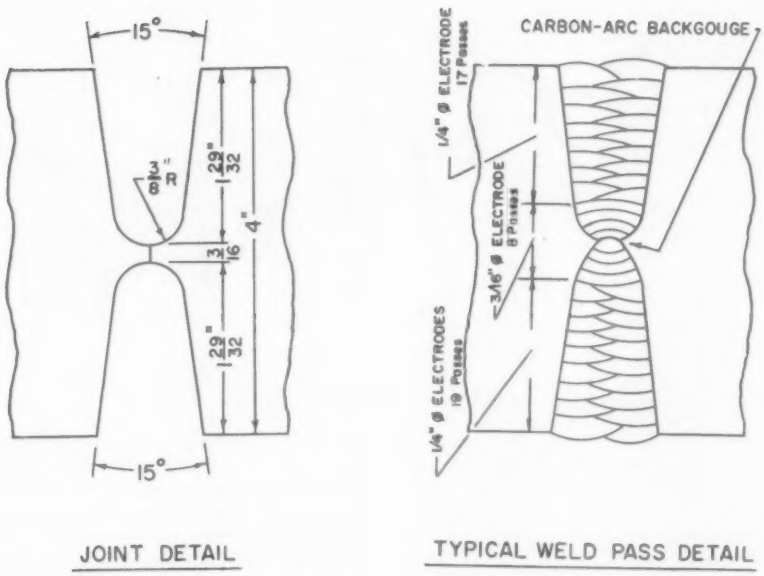

Fig. 1 Details of joint preparation and weld passes

maintained. For preheated assemblies, the temperature was maintained at the desired preheat temperature. Such assemblies were permitted to cool down to room temperature at the end of the workday. Preheat was restored prior to welding on the following day. The root of each weld was carbon-arc gouged prior to depositing metal on the second side. Test-bar blanks were then removed from the welded assembly by sawing in accordance with Fig. 3, subsequently stress relieved when required, and machined to the dimensions shown in Fig. 4.

Duplicate sets of four transverse tensile specimens per set and quadruple sets of three bend specimens per set were selected from each assembly. In connection with the tensile specimens, the 1-in. thickness was chosen in order to keep the breaking load within the capacity of the testing machine. With reference to the side-bend specimens, three specimens were removed from the thickness direction, two of which were 1.5 in. wide and the third less than 1 in. wide. The 1.5-in. width was chosen in order to keep the dimensions as close as possible to those specified by Section IX of the 1952 ASME Code. The 1952 Code was used since the original work on the 1-in. thickness plates was conducted under those limitations. Width of the third specimen was approximately 3/4 in. or, in other words, whatever remained after the first two specimens were removed. Bend tests were performed in a standard jig with a mandrel radius of 3/4 in. except the 90,000 psi yield strength steel which was tested with a 1 1/4-in. radius mandrel.

Table 1—Analysis of 1-In. Thick Steels

		Yield strength	
	A302B	70,000 psi	90,000 psi
C	0.17	0.10	0.14
Mn	1.30	0.54	0.93
Si	0.24	0.23	0.30
S	0.029	0.027	0.020
P	0.020	0.006	0.019
Cr	0.02	0.60	0.19
Ni	0.10	2.28	1.06
Cu	0.21	0.26	0.27
Mo	0.40	0.41	0.46
V	Nil	0.09	0.05
B	...	...	0.007

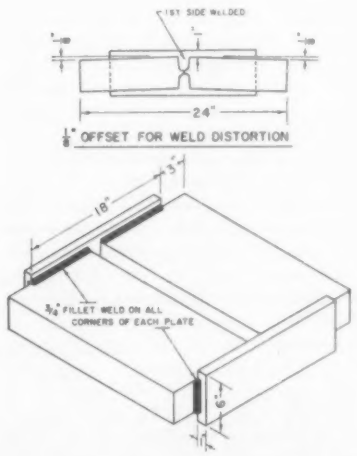

Fig. 2 Layout of plate before welding, showing restraint plate fit-up

Materials

Steels

Since A201A steel has been employed to some extent in the 4-in. thickness for pressure vessels, its use was selected as a standard for comparison with the higher strength steels. The steel was in the normalized condition. In addition, one steel each of 50,000, 70,000 and 90,000 psi minimum yield strength was employed. ASTM A302B, a manganese-molybdenum normalized (and also tempered for the 4-in. plate) steel, was selected for the 50,000 psi yield strength level. For the 70,000 psi yield strength level, a Ni-Cu-Mo-V normalized and tempered steel was selected and for the 90,000 psi yield strength level, a quenched and tempered Mn-Ni-Cr-Cu-Mo-V steel was employed. Chemical analysis as reported by the makers of the steel involved is shown in Tables 1 and 2.

Electrodes

Electrodes employed for procedure qualification tests were the same as were those used in the previous investigation.[4] In addition, where the effects of multiple stress-relief treatments were investigated, E9015 (1.3% Mn – 0.5% Mo) electrodes were substituted for the E10016 and E12015 Ni-Mo-V type. Such a substitution was made owing to the raising of the transition temperature of Ni-Mo-V deposits as a result of stress relief.[5]

Electrode, Preheat and Postheat Variables

The conditions under which the test assemblies were welded and postheat treated may be noted in Table 3. In connection with this table, it may be observed that where stress relief is in-

Table 2—Analysis of 4-In. Thick Steels*

			Yield strength	
	A201A	A302B	70,000 psi	90,000 psi
C	0.15	0.20	0.11	0.15
Mn	0.47	1.40	0.45	0.72
Si	0.19	0.26	0.18	0.26
S	0.27	0.027	0.023	0.016
P	0.13	0.017	0.012	0.016
Cr	...	...	...	0.63
Ni	...	...	2.48	0.86
Cu	...	...	0.65	0.46
Mo	...	0.44	0.54	0.55
V	...	...	0.11	0.05
B	...	...	...	0.003

* Mill Test Reports.

volved, both a 1- and a 4-cycle treatment has been employed. The use of a 4-cycle treatment was based on the premise that where forming conditions are involved such as a dished and flanged multiple-piece head, a cone with a knuckle radius or a transition section, it is likely that plates of an assembly may receive a preliminary treatment before forming, a second or intermediate treatment during forming, a third treatment after the assembly is welded and a fourth or final treatment after the assembly is joined to the remainder of a vessel. As was mentioned earlier, those electrodes which are embrittled as a result of a postheat treatment have been replaced by the E9015 Mn-Mo electrodes.

Of the four steels tested, only two, A201A and A302B, are presently recognized by the ASME Code for use in pressure vessels in the thickness employed here. For those two steels in the 4-in. thickness a mandatory stress relief is required and hence no tests were made without stress relief.

Results

A201A Steel

As may be noted in Table 4, all requirements for Code procedure qualification have been met regardless of preheat and postheat conditions. A photograph of typical tension and bend specimens is shown in Fig. 5. In spite of the success obtained with nonpreheated assemblies, it is pointed out that the results were obtained on an assembly which, although containing considerable restraint, does not necessarily represent the actual conditions encountered in fabrication. In actual practice, even in case of carbon steels, it is sometimes necessary to employ preheat in order to prevent cracking and, indeed, it may be necessary to stress relieve the assembly immediately following completion of welding. The boundary where preheat is not required and where preheat is required cannot be clearly defined and will be decided largely by experience. Therefore, caution should be exercised in direct application of small-scale test results to actual fabrication.

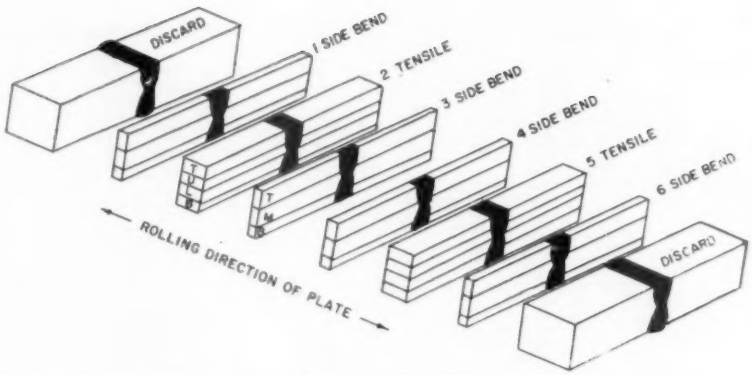

Fig. 3 Order of removal of test specimens from 4-in. plate

Table 3—Electrode, Preheat and Postheat Variables

A201A and A302B	E7016—No preheat—1150° F Stress relief (1 cycle)
	E7016—No preheat—1150° F Stress relief (4 cycles)
	E7016—300° F—1150° F Stress relief (1 cycle)
Ni-Cu-Mo-V	E10016—No preheat—No stress relief
	E10016—300° F—No stress relief
	E9015—No preheat—1150° F Stress relief (1 cycle)
	E9015—No preheat—1150° F Stress relief (4 cycles)
Mn-Ni-Cr-Cu-Mo-V	E12015—No preheat—No stress relief
	E12015—300° F—No stress relief
	E9015—No preheat—1150° F Stress relief (1 cycle)
	E9015—No preheat—1150° F Stress relief (4 cycles)

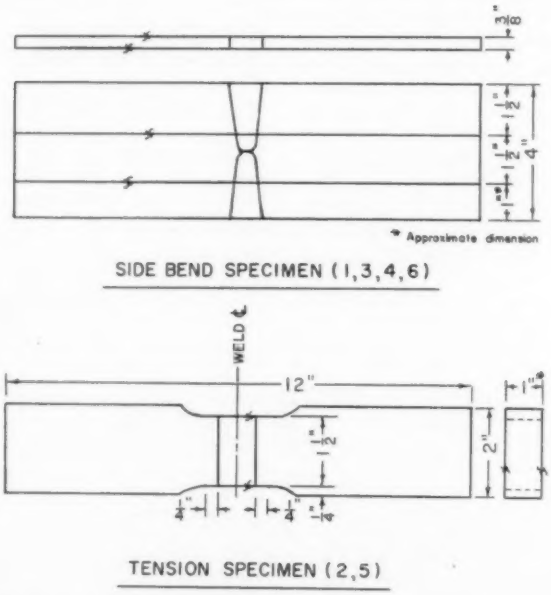

Fig. 4 Details of test specimens

A302 Grade B Steel

In the case of nonpreheated assemblies, no specimens could be tested owing to the presence of cracking which was discovered after stress relief. This cracking, involving both base metal and weld metal, was so extensive as to include virtually all the specimens which were removed from the assembly.

In the case of the preheated assembly, it is obvious from Table 5 that the use of E7016 electrodes is entirely unsatisfactory from the strength standpoint for welding this material which has a minimum strength requirement of of 80,000 psi. The results obtained for the 4-in. thickness are not in agreement with results obtained using E7016 electrodes for 1-in. plate as noted in Table 6. For the 1-in. thickness E7016 electrodes developed strengths around 80,000 psi. The reason for the lowered tensile strength in the case of the 4-in. thickness may be due to the use of $\frac{1}{4}$-in.-electrodes wherein a greater energy input was employed per pass. In view of the inadequate results obtained with E7016 electrodes, it is evident that electrodes of the E8016 or E9015 (Mn-Mo) series are definitely in order for joining A302B steel. Furthermore, the use of preheat is essential if a crack-free weldment is to be obtained. In connection with the presence of cracks, one may appreciate that the expense of making repairs would be high owing to (1) determining the depth of the crack, and (2) making the repair itself, i.e., welding under preheat and restress relieving.

Table 4—Flat-Position Welds in 4-In. A201A Steel

E7016 electrode—No preheat—1 cycle at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	61,400 (P)†	59,600 (P)
U	57,400 (P)	56,900 (P)
L	54,000 (P)	58,400 (P)
B	56,800 (P)	60,800 (P)

E7016 electrode—No preheat—4 cycles at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	59,000 (P)	58,400 (P)
U	57,300 (P)	55,800 (P)
L	58,600 (P)	56,000 (P)
B	57,300 (P)	57,800 (P)

E7016 electrode—300° F preheat—1 cycle at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	61,000 (P)	63,100 (P)
U	60,000 (P)	59,700 (P)
L	58,000 (P)	59,400 (P)
B	63,000 (P)	61,900 (P)

* All side-bend specimens were satisfactory.
† (P) Plate failed.

Table 5—Flat Position Welds in 4-In. A302B Steel

E7016 electrodes—No preheat—1 cycle at 1150° F

Cracked specimens discovered after stress-relieving operation.

E7016 electrode—No preheat—4 cycles at 1150° F

Cracked specimens discovered after stress-relieving operation.

E7016 electrode—300° F preheat—1 cycle at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	77,700 (W)†	75,700 (W)
U	76,400 (W)	77,700 (W)
L	77,200 (W)	75,700 (W)
B	77,900 (W)	75,600 (W)

E9015 electrode—300° F preheat—1 cycle at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	95,400 (P)†	96,100 (P)
U	92,500 (P)	90,100 (P)
L	89,400 (P)	87,900 (P)
B	96,700 (P)	97,200 (P)

* All side-bend specimens were satisfactory.
† (W) Weld failed; (P) Plate failed.

Ni-Cu-Mo-V Steel

As may be noted from Table 7, a high level of strength was obtained along with satisfactory ductility when E10016 electrodes were employed with and without preheat in the absence of stress relief. The level of strength was considerably higher than had been obtained for the 1-in. thickness (Table 8). A manifestation of the high strength of this material may be observed by the fact that failures consistently occurred in the weld.

Where the effect of multiple stress relief was studied, E9015 Mn-Mo electrodes were substituted for the E10016 (Ni-Mo-V) electrodes owing to embrittlement of Ni-Mo-V deposits as a result of stress relief.[5] It may be observed that failures consistently occurred in the weld, as was the case for E10016 electrodes, at a relatively high strength level. The strength after a four-cycle stress-relief treatment is slightly lower than that obtained with a single stress-relief treatment.

It is also of interest to note that a general tendency exists for the "T" and "B" (top and bottom quarters, respectively) specimens which have the greatest width of weld, to exhibit somewhat greater strengths than specimens "U" and "L" removed from the middle of the plate (in the thickness direction). Such a condition might not necessarily be exhibited, however, if failures occurred in the plate.

Table 6—Flat-Position Welds in 1-In. A302B Steel

Electrode	E7016	E7016	E7016
Preheat	None	None	300° F
Postheat	1150° F (1 cycle)	1150° F (4 cycle)	1150° F (1 cycle)
Tensile, psi	83,000 (P)* 84,000 (P)	80,300 (W)* 80,800 (W)	82,000 (W) 81,000 (W)
Side bends	4—OK	4—OK	4—OK

* (W) Weld failed. (P) Plate failed.

Fig. 5 Typical tensile and bend-test specimens

Mn-Ni-Cr-Cu-Mo-V Steel

In direct contrast to the Ni-Cu-Mo-V steel, the strength of this quenched and tempered Mn-Ni-Cr-Cu-Mo-V steel exhibited lowered strength for the 4-in. thickness as compared to the 1-in. thickness as may be noted from Tables 9 and 10. In some cases, the ultimate tensile strength has dropped below the usually specified 105,000 psi minimum.

Table 7—Flat-Position Welds in 4-In. Ni-Cu-Mo-V Steel

E10016 electrode—No preheat—No stress relief*

Specimen Location	*Tensile Strength, psi*	
T	106,400 (W)†	104,200 (W)
U	111,600 (W)	108,500 (W)
L	116,800 (W)	116,500 (W)
B	100,200 (W)	101,000 (W)

E10016 electrode—300° F preheat—No stress relief*

Specimen Location	*Tensile Strength, psi*	
T	108,500 (W)	106,100 (W)
U	113,600 (W)	114,800 (W)
L	113,600 (W)	115,800 (W)
B	104,900 (W)	105,500 (W)

E9015 electrode—No preheat—1 cycle at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	113,500 (W)	116,800 (W)
U	120,500 (W)	116,400 (W)
L	116,800 (W)	118,500 (W)
B	113,600 (W)	112,800 (W)

E9015 electrode—No preheat—4 cycles at 1150° F*

Specimen Location	*Tensile Strength, psi*	
T	109,100 (W)	110,000 (W)
U	111,000 (W)	113,400 (W)
L	116,300 (W)	112,500 (W)
B	109,100 (W)	108,800 (W)

* All side-bend specimens were satisfactory.
† (W) Weld failed.

Table 8—Flat-Position Welds in 1-In. Ni-Cu-Mo-V Steel

Electrode	E10016	E10016	E9015	E9015
Preheat	None	300° F	None	None
Postheat	None	None	1150° F (1 cycle)	1150° F (4 cycles)
Tensile, psi	100,350 (P)*	101,000 (P)	99,500 (P)	98,200 (P)
	100,300 (P)	100,200 (P)	98,200 (P, W)*	98,200 (P)
Side bends	4—OK	4—OK	4—OK	4—OK

* (P) Plate failed; (W) weld failed.

The subminimum values for those specimens failing in the plate are still within 95% of the minimum specified tensile strength and as such are considered acceptable by the 1952 ASME Code.

It may be observed in Table 9 that the use of E12015 electrodes under no preheat and without stress relief resulted in satisfactory welds. In the case of E9015 electrodes employed with no preheat and a single stress-relief treatment, fusion line cracks were observed after testing and those cracks account for the exceptionally low strength in the two top specimens. Specimens treated with multiple stress-relief cycles resulted in adequate strength and ductility.

Table 9—Flat-Position Welds in 4-In. Mn-Ni-Cr-Cu-Mo-V Steel

E12015 electrode—No preheat—No stress relief*

Specimen Location	*Tensile Strength, psi*	
T	106,800 (P)†	107,500 (P)
U	104,800 (P)	107,300 (P)
L	103,300 (P)	104,700 (P)
B	109,700 (P)	107,500 (P)

E12015 electrode—300° F preheat—No stress relief*

Specimen Location	*Tensile Strength, psi*	
T	106,600 (P)	104,700 (P)
U	103,900 (P)	101,000 (P)
L	105,000 (P)	105,000 (P)
B	103,100 (P)	102,400 (P)

E9015 electrode—No preheat—1 cycle at 1100° F*

Specimen Location	*Tensile Strength, psi*	
T	86,200 (F)†	89,900 (F)
U	101,700 (P)	101,400 (P)
L	104,600 (P)	106,200 (W, P)
B	104,700 (W)†	104,900 (W)

E9015 electrode—No preheat—4 cycles at 1100° F*

Specimen Location	*Tensile Strength, psi*	
T	108,800 (W)	107,800 (W)
U	104,800 (P)	104,200 (P)
L	101,400 (P)	105,300 (P)
B	109,300 (W)	108,700 (W)

* All side bends were satisfactory (2.5-in. diam. mandrel).

† (P)plate failed; (W) weld failed; (F) fusion line crack was observed.

Conclusions

For the particular conditions of this investigation, the following conclusions are warranted:

1. Welds made in A201A steel, with or without the application of 300° F preheat, followed by stress relief at 1150° F, meet the requirements of the 1952 ASME Code when E7016 electrodes are employed. Results are also satisfactory when multiple stress relief is performed.

2. The 1-in. thickness, A302B steel meets Code requirements for welding when joined with E7016 electrodes with or without 300° F preheat using either single or multiple stress relief. For the 4-in. thickness, crack-free welds could not be obtained without the use of preheat. Moreover, the E7016 electrode does not have adequate strength to meet the Code specifications. The use of E9015 (Mn-Mo) electrodes and 300° F preheat is recommended along with a stress-relief treatment.

3. Both preheated and nonpreheated welds made with E10016 (Ni-Mo-V) electrodes are satisfactory for joining Ni-Cu-Mo-V steel when no stress relieving operation is performed. For welding this same steel, the use of E9015 Mn-Mo electrodes without preheat, followed by either single or quadruple stress relief at 1150° F, is adequate for meeting Code welding requirements. In connection with this normalized and tempered steel, the 4-in. thickness was shown to have a definitely higher ultimate strength than the 1-in. thickness.

4. The quenched and tempered Mn-Ni-Cr-Cu-Mo-V steel in the 1-in and 4-in. thickness can be joined successfully using E12016 (Ni-Mo-V) electrodes with or without the use of preheat or stress relief. There are indications, however, that consistently crack-free welds for the 4-in. thickness cannot always be obtained without preheat when E9015 electrodes are employed. Crack-free welds (E9015) will provide adequate strength and ductility when a stress relief treatment is employed. In connection with strength, this steel has demonstrated a lower ultimate strength in the 4-in. thickness than in the 1-in. thickness.

Generally speaking, all the steels welded indicated good mechanical properties when preheat was employed. In some cases, the requirements of the ASME Code were met where neither preheat nor postheat was employed. In spite of such encouraging results from 4-in. thick nonpreheated assemblies, there are indications that cracking, originating at unavoidable notches, can result from slight deformations produced by weld metal shrinkage. Recent published literature[6] has also indicated that severe cracking may be produced in thick plates as a result of residual stresses. In view of such conditions, the selection of welding procedures involving no preheat should be made with considerable caution. In that connection, judgment based on experience will decide when and to what extent preheating is necessary and also whether or not immediate stress relief following welding will be required.

Acknowledgment

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Table 10—Flat-Position Welds in 1-In. Mn-Ni-Cr-Cu-Mo-V Steel

Electrode	E12015	E12015	E9015	E9015
Preheat	None	300° F	None	None
Postheat	None	None	1100° F (1 cycle)	1100° F (4 cycles)
Tensile, psi	122,000 (P, W)*	119,500 (W)	109,200 (W)	113,000 (W)
	123,000 (W)	120,500 (W, HAZ)*	112,200 (W)	108,600 (W)
Side Bends	4—OK	4—OK†	4—OK†	4—OK

* (P) Plate failed; (W) weld failed; (HAZ) heat-affected zone failed.
† 2.5-in. diam. mandrel.

HEAT TREATMENT OF CARBON AND LOW-ALLOY PRESSURE-VESSEL STEELS

PVRC interpretive report describes code requirements, recommended heat-treatment practices and forming procedures for pressure-vessel steels

BY O. R. CARPENTER AND C. FLOYD

SUMMARY. Heat treatment of one type or another is often required in the course of manufacturing a pressure vessel. This may include preliminary normalizing, quenching or annealing, various intermediate treatments or final heat treatment which usually comprises stress relief or tempering. This report describes the requirements prescribed by the ASME Boiler and Pressure Vessel Code for the various steels commonly used for pressure vessels, together with the reasons underlying these requirements. Recommended practice for each of the various classes of steels is described in detail. Since cold and hot forming procedures are intimately related to heat treating practices, considerable attention is devoted to the details of suitable forming practices and the heat-treating procedures which may be associated with them.

Materials Division
Pressure Vessel Research Committee

1. Introduction

This report has been prepared to summarize the present knowledge of heat treatment of pressure-vessel steels and to point out gaps in the present knowledge of this subject which require further investigation and careful study.

In the subsequent sections the pressure-vessel steels are divided into groups primarily based on their chemical compositions. For each group, a detailed discussion is then given of the heat treatment prior to cold or hot forming operations, as well as any intermediate heat treatments that may be required during fabrication of the pressure vessels; and, finally, a recommendation is made for the final treatment of the pressure vessels in that group.

The authors wish to point out that, with the exception of carbon-steel pressure vessels, the published data on this subject is scant; therefore, we are presenting information that is based primarily on the general heat-treating practices employed by most fabricators of pressure vessels, as well as those practices used by the plate manufacturers.

The authors wish to emphasize that the details of the various fabrication operations are common practices employed by several fabricators, but are not presented with the intent of standardizing shop practices—merely as general information.

The requirements of the ASME Boiler and Pressure Vessel Code (1956 Edition) are given below:

Section I, "Rules for Construction of Power Boilers," of the ASME Boiler and Pressure Vessel Code states the basic requirements for stress relieving of power boilers in Paragraph P-108, which are as follows: "Except as specifically provided otherwise in the Code, all fusion-welded pressure parts of power boilers shall be stress relieved. It shall be done by heating uniformly to at least 1100° F, and up to 1200° F or higher, if this can be done without distortion. Different temperatures may be used to obtain proper stress relieving when required by the characteristics of the material."

Section VIII, "Rules for Construction of Unfired Pressure Vessels," of the ASME Boiler and Pressure Vessel Code, gives the following detailed information on thermal stress relief in Paragraph UCS-56 of Subsection C for carbon and low-alloy steels: "Vessels or parts of vessels shall be thermally stress relieved by one of the procedures given in Paragraph UW-40 when the plate thickness, including corrosion allowance, at any main welded joint in the vessel shell or head exceeds any of the following values, or when required by Paragraph U-1(e), UCS-67 or UW-2:

"(1) Zero thickness for steels conforming to Specifications SA-301 Grade B, SA-302, SA-217 Grades WC4 and WC5, SA-357, SA-387 Grades B, C, D and E, and for chromium-molybdenum steel having a chromium content greater than 0.70%;

"(2) 0.58 in. for steels conforming to Specifications SA-202, SA-203, SA-204, SA-225, SA-299, SA-301 Grade A, SA-387 Grade A, and for any steel having a specified molybdenum content of 0.50% nominal (permissible range 0.40 to 0.65%) with a chromium content not greater than 0.70%;

"(3) 1.00 in. for steels conforming to Specifications SA-212, SA-105 Grade II, SA-181 Grade II, SA-266 Grade II, SA-95 and SA-216 Grade WCB;

"(4) 1.25 in. for all other steels that may be used in welded construction;

"(5) A thickness greater than $(D + 50 \text{ in.})/120$ where D = inside diameter, in., used in determining the required thickness of the vessel or vessel part, or 20 in., whichever is greater.

. . . "A minimum temperature of 1100° F* shall be maintained for a period of 1 hr per inch of metal thickness (maximum metal thickness of the shell or head plate)."

* When it is impracticable to stress relieve at a temperature of 1100° F, it is permissible to carry out the stress relieving operation at lower temperatures for longer periods of time in accordance with the following tabulation:

Metal temperature, ° F.	*Holding time, hr per in. of thickness*
1100	1
1050	2
1000	3
950	5
900	10

For intermediate temperatures, the holding time shall be determined by straight-line interpolation.

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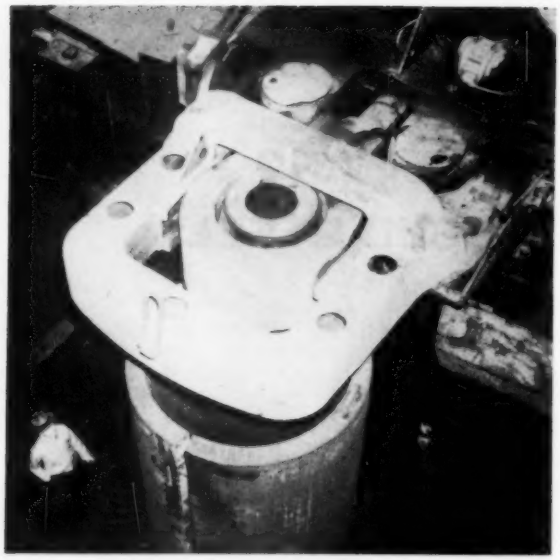

Fig. 1 Top view of vertical rolls used for hot or cold forming shells up to 10 ft in length into cylindrical shape

Fig. 2 Press for cold forming of carbon-steel or heat-treated alloy-steel plates

In order to prevent any confusion in the various types of heat treatments discussed in this report, the following basic definitions† are given.

Anneal: Slow cool from above the upper critical temperature or A_3 temperature. The annealing temperature for carbon and low-alloy steels is in the range of 1550 to 1675° F, depending on the type of steel. The steel is slowly cooled to 1200° F for carbon steels and possibly as low as 1000° F for some of the low-alloy steels. The rate of cooling below the above temperatures depends on the type, geometry and size of the material being heat treated. This heat treatment is generally called a full anneal.

Normalize: Cool in still air from above the upper critical temperature or A_3 temperature. If a furnace load is removed from the furnace on a car, it is mandatory that the furnace load be removed from the car as quickly as possible and the plates or parts cooled in air. The normalizing temperature for carbon and low-alloy steels is in the range of 1600 to 1800° F, depending on the type of steel.

Quench: Rapid cool in liquid from above the upper critical temperature or A_3 temperature.

Stress Relief, Temper or Postheat Treatment: Heat to a temperature not exceeding the lower critical temperature or A_1 temperature. Stress relieving or tempering heat treatments generally range from 1100 to 1350° F, depending on the type of steel, except as noted for Section VIII, "Rules for Construction of Unfired Pressure Vessels." The rate of cooling from the stress relieving temperature depends on the type, geometry and size of material being heat treated. This heat treatment is sometimes called a process anneal, subcritical anneal or postheat treatment after welding.

Plates may be purchased from the mill in the hot-rolled, annealed, normalized, normalized-and-tempered or quenched-and-tempered condition, depending on the type of steel and the particular facilities available to the fabricator to perform various types of hot or cold forming operations, heat treating equipment and other similar factors. Figure 1 shows vertical rolls used for hot and cold forming of shells. Figure 2 illustrates a press for cold forming of carbon or heat-treated alloy-steel plates.

The limits for cold forming depend on the thickness of the plate and the radius to which the plate is to be bent. The radius of bending is generally based on a maximum percent strain on the outer fibers of the plate or an outer fiber stretch of 3% to 4½%, depending on the material being cold formed, without requiring an intermediate stress-relieving heat treatment.

Strain on outer fibers of plate or outer fiber stretch may be calculated as follows:

$$S = \frac{R_1R_2 - \frac{1}{2}R_1T}{R_1R_2 - \frac{1}{2}R_2T} - 1$$

or

$$\%S = S \times 100\%$$

where

S = strain.
R_1 = final radius.
R_2 = original radius (for flat plate use a value of 1000 in.).
T = thickness of plate.

Other important factors that control the maximum percent strain on the outer fibers are plate width, direction of bending or whether the plate is formed at room temperature or heated to some temperature below 1150° F (in the case of some of the low-alloy steels) just prior to cold forming. Cold forming at room temperature in the winter may be quite different than performing the same operation in the summer.

Although an outer fiber stretch of 3 to 4½% may seem low, it should be recognized that this figure represents an average strain along a smooth surface, whereas the usual plate surface invariably contains some abrupt depressions where the actual local strain can considerably exceed 3 to 4½%. Failure to allow for excessive local straining during the cold forming operation may cause local ruptures to occur in the plate. Under some circumstances local ruptures could act as a triggering mechanism to produce cracks and possible failure of the plate during forming.

If the cold forming operation requires more than 3 to 4½% outer fiber stretch, depending on the material being cold formed, it is a general practice to cold form in two or more stages with the application of an intermediate stress-relief heat treatment when the first stage of cold forming has approached the 3 to 4½% outer fiber stretch.

In many cases the ability of the fabricator to cold or hot form plates may depend on the equipment and other facilities available for such operations.

2. Heat Treatment of Carbon-Steel Pressure Vessels

The following ASME Specifications cover the flange and firebox quality plates for carbon-steel pressure vessels:

SA-201. Carbon-Silicon Steel Plates

† See also standard definitions of metallurgical terms, ASM Metals Handbook.

of Intermediate Tensile Ranges for Fusion-Welded Boilers and Other Pressure Vessels.

SA-212. High-Tensile Strength Carbon-Silicon Steel Plates for Boilers and Other Pressure Vessels.

SA-30. Boiler and Firebox Steel for Locomotives.

SA-285. Low- and Intermediate-Tensile Strength Carbon-Steel Plates of Flange and Firebox Qualities.

SA-30 and SA-285 specifications, representing low and intermediate tensile strength plates, make no references to heat treatment other than the mention of stress relief (1100 to 1200° F) of test specimens when so specified on the purchase order with the mill. SA-30 and SA-285 plates are limited to a maximum thickness of 2 in. and are generally tested at the mill in the hot-rolled condition.

SA-201 and SA-212 plates contain the same requirements as SA-30 and SA-285 for plates 2 in. and under in thickness, i.e., the material is tested in the hot-rolled condition unless the purchase order with the mill specifies that the test specimens be stress relieved at a temperature of 1100 to 1200° F.

SA-201 and SA-212 specifications contain a special section entitled "Heat Treatment," which is as follows: "Plates over 2 in. in thickness shall be treated to produce grain refinement either by normalizing or heating uniformly for hot forming. If the required treatment is to be obtained in conjunction with the hot-forming operation, the temperature to which the plates are heated for hot forming shall be equivalent to and shall not significantly exceed the normalizing temperature."

Cold Forming of Carbon-Steel Plates and Heads

The maximum thickness permissible for cold forming is dependent on the radius to which the plate or head is to be bent. This radius is generally based on producing a maximum outer fiber stretch of 4 to 4 1/2% in the plate or head without requiring an intermediate stress-relieving heat treatment. If the cold forming operation requires more than 4 to 4 1/2% outer fiber stretch, it is a general practice to give an intermediate stress-relieving heat treatment after each increment of outer fiber stretch of 4 to 4 1/2% in the series of cold forming operations. Experience indicates that plates less than 1 3/4 in. thick can be cold formed without normalizing prior to the cold-forming operation if proper equipment is available. The hot finishing temperature at the mill for plates less than 1 3/4 in. thick is usually sufficiently low to give a fine-grained structure which is equivalent to a normalizing heat treatment.

When plates are within the thickness range of 1 3/4 to 2 1/2 in., they can sometimes be cold formed if it is a fine-grained material and the equipment is suitable for this work. The grain size may be checked by microexamination of a sample removed from the plate. If not fine grained, the plate may be normalized before cold forming. Normalizing is generally performed at 1600 to 1650° F with a holding time of 1 hr per inch of thickness in this temperature range followed by air cooling.

The above information on plates in the thickness range of 1 3/4 to 2 1/2 in. generally applies to the cold forming of plates exceeding 2 1/2 in. in thickness where circumstances require this type of forming operation.

Heads are usually hot pressed rather than cold formed. The exceptions to this statement will be relatively thin heads of suitable diameter such that no tearing or wrinkling can occur in the head during the cold-forming or cold-pressing operation.

Hot Forming of Carbon-Steel Plates and Heads

Procedures for heating plates and heads should be so established that a plastic plate is obtained for the hot forming or pressing operation without evidence of oxidation in the form of severe decarburization and/or the formation of a network of oxide penetrations or surface checking known as "chicken-wire" cracking, which can cause permanent damage to the plate surfaces.

The following hot-forming temperatures have been successfully used:

Plate thickness, in.	*Hot forming temperature, ° F*
Up to 3 in.	1650
Over 3 in., up to 4 1/2 in.	1750
Over 4 1/2 in.	1850

The above temperatures are predicated on the use of a holding time of sufficient length at a temperature of 1500 to 1650° F to insure a suitably plastic plate for hot forming or pressing.

Some fabricators may prefer a higher hot-forming temperature range, possibly as high as 1800 to 2000° F. This procedure requires a rapid heating rate to this temperature range and a holding time of very short duration to avoid the possibility of decarburization or "chicken-wire" cracking at these high hot forming temperatures.

Where the lower hot-forming temperatures are used, the plates may remain fine grained and not require a normalizing heat treatment. This may be verified by a microexamination of a sample from the hot-formed plate. With regard to the higher hot-forming temperatures, experience indicates that plates so heated are coarse grained, in most cases, after hot forming, and generally require a normalizing heat treatment after this operation. The normalizing heat treatment used by most fabricators is heating to 1600 to 1650° F, holding in this temperature range for 1 hr per inch of thickness, followed by air cooling.

Hot formed plates are cold formed or "cold sized" to obtain the required tolerance on circularity. No heat treatment is required after this cold-sizing operation since the outer fiber stretch is much less than 4 or 4 1/2%; in fact, in most cases the outer fiber stretch is less than 1%.

Heavy head plates may be hot pressed in the temperature range of 2100 to 2150° F. This heating operation involves rapid heating to this temperature range with a very short holding time to avoid decarburization and/or "chicken-wire" cracking. This applies particularly to head plates over 2 in. thick. Heads that are less than 2 in. thick may be hot pressed in the temperature range of 1800 to 2000° F. It is a general practice to normalize all heads that are 2 in. or more in thickness. This normalizing heat treatment is identical to that outlined in the preceding paragraph for plates.

Intermediate Heat Treatments During Fabrication of Carbon-Steel Pressure Vessels

Any intermediate heat treatments required during the fabrication of carbon-steel pressure vessels, after hot- and/or cold-forming operations are completed, are generally performed in the temperature range of 1100 to 1200° F. The intermediate heat treatments required are determined by the thickness of the section, the carbon content of the plate and similar factors.

Final Heat Treatment of Carbon-Steel Pressure Vessels

It is common practice to apply a final heat treatment, after fabrication is completed, in accordance with the requirements of Section I or Section VIII of the ASME Boiler and Pressure Vessel Code. If a final heat treatment is required, the vessel may be heated slowly to a temperature range of 1100 to 1200° F, held 1 hr per inch of thickness, and cooled slowly in a still atmosphere to a temperature not exceeding 600° F. Figure 3 is a view of a large heat-treating furnace showing vessels in various stages of fabrication after a stress-relieving heat treatment.

3. Heat Treatment of Carbon-Molybdenum-Steel Pressure Vessels —ASME SA-204

SA-204 contains specifications for flange and firebox quality plates for carbon-molybdenum-steel pressure vessels. The general information outlined in Section 2 of this report on the

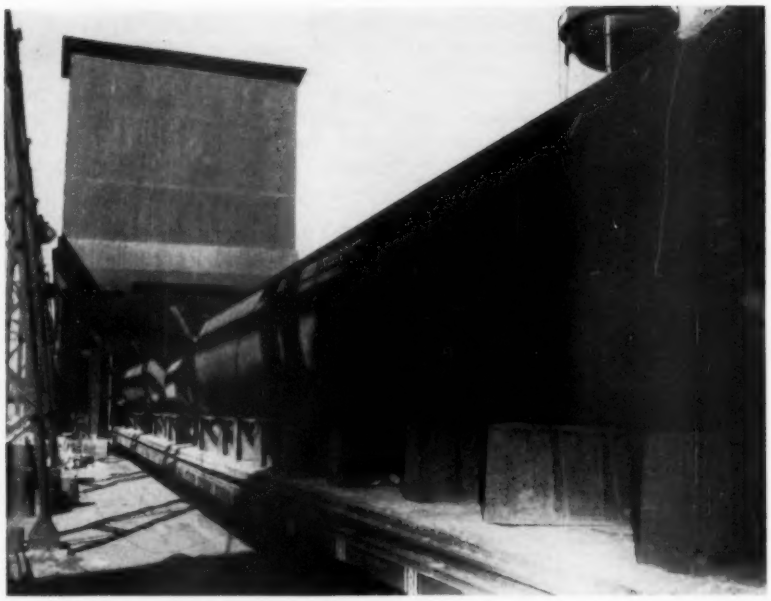

Fig. 3 View of large heat-treating furnace showing vessels in various stages of fabrication after a stress-relieving heat treatment. This furnace may also be used for normalizing heat treatments

heat-treatment procedures after cold or hot forming of plates and heads applies equally well to carbon-molybdenum pressure vessels except that some fabricators may require a stress-relief heat treatment after normalizing for Grade C (75,000 psi tensile strength) plates or heads. The same general comments on intermediate heat treatments for carbon steels in Section 2 are applicable to carbon-molybdenum steels.

Some fabricators use a slightly higher final heat-treating temperature range for carbon-molybdenum than for carbon-steel pressure vessels. If the ASME Boiler and Pressure Vessel Code specifies that the carbon-molybdenum-steel pressure vessel receive a final heat treatment after fabrication is completed, it is common practice to heat the vessel slowly to a temperature range of 1100–1250° F, hold at the specified temperature for 1 hr per inch of thickness and cool slowly in a still atmosphere to a temperature not exceeding 600° F.

4. Heat Treatment of Manganese-Molybdenum-Steel Pressure Vessels —ASME SA-302

SA-302 specification describes the flange and firebox quality plates for the manganese-molybdenum-steel pressure vessels. The majority of the pressure vessels made of plates of this specification use Grade B or 80,000 psi min tensile strength plate.

SA-302 plates may be purchased in the hot-rolled or annealed or normalized and stress-relieved condition, depending on the method of fabrication or customer requirements.

Cold Forming of Manganese-Molybdenum-Steel Plates and Heads

Experience indicates that all SA-302 plates should be annealed or preferably be normalized and stress relieved prior to cold forming. The maximum thickness permissible for cold forming is dependent on the radius to which the plate or head is to be bent. This radius is commonly based on producing a maximum outer fiber stretch of 3% in the plate or head, without requiring an intermediate stress-relief heat treatment. Other equally important factors that control the maximum percent strain on the outer fibers are discussed in Section 1 of this report and shall certainly be considered for manganese-molybdenum steels.

If the outer fiber stretch of the plate exceeds 3%, the cold-forming operation is generally done in two or more stages with the application of an intermediate heat treatment of 1125 to 1250° F when the first stage of cold forming has approached the 3% outer fiber stretch.

Hot Forming of Manganese-Molybdenum-Steel Plates and Heads

The hot forming of SA-302 plates and heads is usually performed in accordance with the general procedure outlined in Section 2 of this report for carbon steel. It is common practice either to anneal or normalize and stress relieve all plates and heads, regardless of thickness, after hot forming before proceeding with further fabrication operations. Hot formed plates are cold formed or "cold sized" to obtain the required tolerance on circularity. No heat treatment is required after this cold forming operation since the outer fiber stretch is usually less than 1%. Figure 4 shows a large 6-in. thick plate of SA-302 steel ready to be removed from a heating furnace for hot forming. Figure 5 illustrates the actual forming operation.

Annealing or Normalizing and Stress-Relieving Heat Treatments

The annealing heat treatment[7] may be specified as follows: Heat slowly to 1550–1600° F, hold at this temperature 1 hr per inch of thickness and

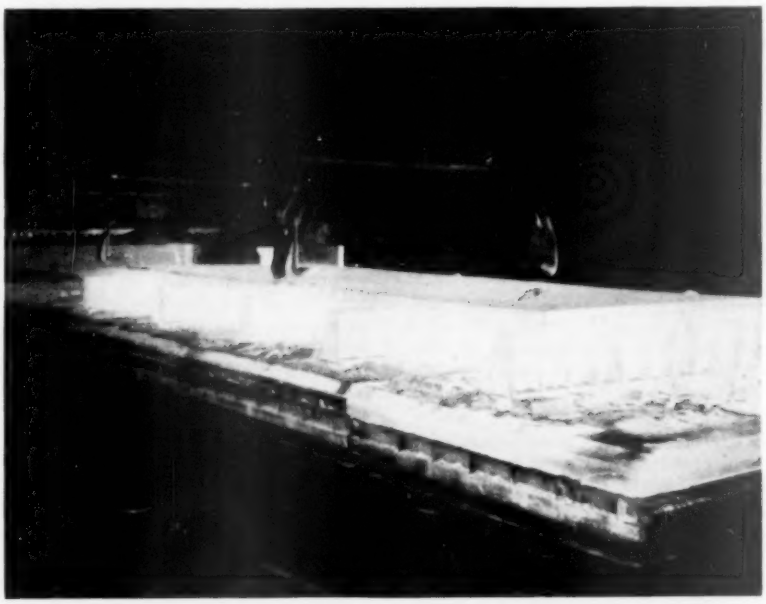

Fig. 4 Six-inch thick plate of SA-302 Grade B material, approximately 35 ft long, ready to be removed from heating furnace for hot forming into a half shell

slowly cool to 1200° F, followed by cooling in still air.

Normalizing and stress-relieving heat treatment is often performed because higher impact values are obtained than with the full anneal. The normalizing temperature may vary from 1600 to 1800° F, depending on whether the plates are purchased to regular mill practice or fine-grain steel melting practice. If the plates are purchased to regular SA-302 specification, 1600 to 1700° F is the normalizing temperature often used; whereas, plates purchased to SA-302 made to a fine-grain deoxidation practice may be normalized at a higher temperature range. Since the plate made to fine grain practice is killed with a greater percent of aluminum than normally used for regular SA-302 material, the higher normalizing temperature will aid in breaking up the "banding" and will increase the tensile strength of the plates and heads. The stress relieving temperature after normalizing may range from 1125 to 1250° F, depending on the tensile strength obtained on the normalized plate. Some fabricators check the microstructure and hardness of the plates and heads after the normalizing heat treatment to be certain that the material is fine grained and the hardness is sufficiently high so that the subsequent stress relief will not lower the tensile strength below the minimum specified for the grade of SA-302 that is being fabricated.

Intermediate Heat Treatments of Manganese-Molybdenum-Steel Pressure Vessels

Any intermediate heat treatments required during the fabrication of manganese-molybdenum-steel pressure vessels, after hot and/or cold forming operations are completed may be satisfied by a temperature range of 1125 to 1250° F. The plate thickness and fabricating procedure used will determine the need for intermediate heat treatments.

Final Heat Treatment of Manganese-Molybdenum-Steel Pressure Vessels

Both Sections I and VIII of the ASME Code specify that SA-302 pressure vessels shall be given a stress-relief heat treatment. Experience indicates that the pressure vessel shall be slowly heated to a temperature range of 1125 to 1250° F, held 1 hr per inch of thickness, and cooled slowly in a still air atmosphere to a temperature not exceeding 600° F.

5. Heat Treatment of Manganese-Steel Pressure Vessels

This group includes the plate specifications that contain more than 0.90% manganese (except SA-302) as the major alloying element. The following ASME specifications describe plates of flange and firebox quality for use in boilers and other pressure vessels:

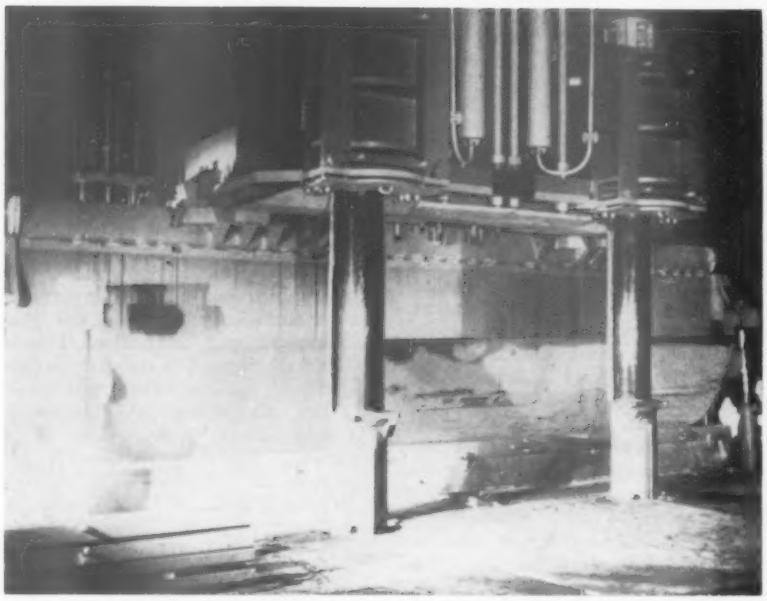

Fig. 5 Plate shown in Fig. 4 being hot formed on a 6500-ton press

SA-202. Chrome-Manganese-Silicon Alloy-Steel Plates for Boilers and Other Pressure Vessels.

SA-225. Manganese-Vanadium Steel Plates for Boilers and Other Pressure Vessels.

SA-299. High-Tensile Strength Carbon-Manganese-Silicon Steel Plates for Boilers and Other Pressure Vessels.

The general information presented for manganese-molybdenum steel plates in Section 4 of this report on the heat-treatment procedures after cold or hot forming of plates and heads is generally considered by most fabricators to apply to this group of plate materials. The same general requirements for intermediate heat treatments for manganese-molybdenum steel plate in Section 4 are applicable to the steel plates in this group. If the ASME Boiler and Pressure Vessel Code specifies that pressure vessels made of SA-202, SA-225 or SA-299 plates receive a final heat treatment after fabrication is completed, it is recommended that the vessel be heated slowly to a temperature range of 1100 to 1250° F, held at the specified temperature for 1 hr per inch of thickness and cooled slowly in a still atmosphere to a temperature not exceeding 600° F.

6. Heat Treatment of Chromium-Molybdenum-Steel Pressure Vessels

ASME Specification SA-301 covers $^1/_2$% chromium – $^1/_2$% molybdenum and 1% chromium – $^1/_2$% molybdenum steel plates, while SA-357 covers 5% chromium – $^1/_2$% molybdenum steel plates.

ASME Specification SA-387 covers five grades of chromium-molybdenum steel plates ranging from $^1/_2$% chromium – $^1/_2$% molybdenum to 3% chromium – 1% molybdenum. Group I is composed of three grades containing not more than 1.5% chromium and designated A, B and C. Group II consists of two grades containing between 2 and 3$^1/_4$% chromium and designated D and E. The various grades have the following general chemical compositions:

Grade	*Chromium*, %	*Molybdenum*, %
A	$^1/_2$	$^1/_2$
B	1	$^1/_2$
C	1$^1/_4$	$^1/_2$
D	2$^1/_4$	1
E	3	1

It will be noted that Grades A and B of SA-387 are comparable to Grades A and B of SA-301. Group II plates and SA-357 plates are intended for mildly corrosive service, as well as for elevated-temperature service.

Heat Treatment of Group I Plates (Grades A, B and C)

SA-387 states the following: "All plates shall be treated to produce grain refinement either by annealing or normalizing and tempering or heating uniformly for hot forming followed by tempering. If the required treatment is to be obtained in conjunction with the hot-forming operation, the temperature to which the plates are heated for hot forming shall be equivalent to and shall not significantly exceed the annealing or normalizing temperature."

Heat Treatment of Group II (Grades D and E) and SA-357 Plates

SA-387 states the following: "All

plates in Group II shall be annealed or normalized and tempered by the plate manufacturer. The tempering temperature shall not be less than 1200° F. Heat treatment involving quenching in a liquid medium is not permitted."

SA-357 specifies that all plates shall be annealed.

Cold Forming of Chromium-Molybdenum-Steel Plates and Heads

Chromium-molybdenum-steel plates may be heat treated either by annealing or normalizing and tempering, depending on the particular specification, prior to cold forming. Other equally important factors that control the maximum percent strain on the outer fibers are discussed in Section 1 of this report and shall certainly be considered for chromium-molybdenum steels. The maximum thickness permissible for cold forming is dependent on the radius to which the plate or head is to be bent. Most fabricators specify that this radius be based on a maximum outer fiber stretch of 3% in the plate or head, without requiring an intermediate stress-relief heat treatment.

If the outer fiber stretch of the plate exceeds 3%, the cold-forming operation is generally performed in two or more stages with the application of an intermediate heat treatment of 1150 to 1300° F when the first stage of cold forming has approached the 3% outer fiber stretch. Heads are generally hot pressed rather than cold formed. The exceptions to this statement will be relatively thin heads of a suitable diameter that no tearing or wrinkling can occur in the head during the cold-forming or cold-pressing operation.

Hot Forming of Chromium-Molybdenum-Steel Plates and Heads

The hot forming of SA-387, SA-301 and SA-357 chromium-molybdenum steel plates and heads is usually performed in accordance with the general procedure outlined in Section 2 of this report for carbon steel. It is common practice either to anneal or normalize and stress relieve all plates and heads, regardless of thickness, after hot forming before proceeding with further fabrication operations. Hot formed plates are cold formed or "cold sized" to obtain the required tolerance on circularity. No heat treatment is required after this cold-forming operation since the outer fiber stretch is generally less than 1%.

Annealing or Normalizing and Tempering Heat Treatments

The annealing heat treatment[7] may be specified in the following manner for the chromium-molybdenum steels:

Heat slowly to 1550–1675° F, hold at this temperature 1 hr per inch of thickness and slowly cool to 1200° F, followed by cooling in still air.

The normalizing and tempering heat treatments vary widely for the different groups of chromium-molybdenum plates. The normalizing temperature for Grades A, B and C (1/2% Cr – 1/2% Mo to 1 1/4% Cr – 1/2% Mo) of Group I of SA-387 may be from 1625 to 1725° F. Although there may be cases, particularly for Grade A, where the normalized plate will meet the tensile requirements without a tempering heat treatment, it is generally considered good practice to apply a tempering heat treatment after normalizing. This tempering heat treatment may vary from 1125 to 1300° F depending on the grade of plate.

The normalizing temperature for Grades D and E of SA-387 (2 1/4% Cr – 1% Mo and 3% Cr – 1% Mo) and SA-357 (5% Cr – 1/2% Mo) may be from 1650 to 1750° F. The tempering heat treatment after normalizing may vary from 1200 to 1350° F depending on the grade of plate.

Some manufacturers and fabricators check the microstructure and hardness of the plates and heads after the normalizing heat treatment to make certain that the material is fine grained and the hardness value thus obtained may be used as a guide for the subsequent tempering heat treatment.

Intermediate Heat Treatments of Chromium-Molybdenum-Steel Pressure Vessels

Any intermediate heat treatments required during the fabrication of chromium-molybdenum-steel pressure vessels, after hot- and/or cold-forming operations are completed, may be in the temperature range of 1125 to 1350° F depending on the type or grade of chromium-molybdenum steel plate. The frequency of such intermediate heat treatments will be determined by the specific fabrication procedure and the plate thickness of the pressure vessels.

Final Heat Treatment of Chromium-Molybdenum-Steel Pressure Vessels

Section I of the ASME Code requires that all chromium-molybdenum-steel pressure vessels be stress relieved. Section VIII specifies that all chromium-molybdenum-steel pressure vessels of Grade B or higher alloy content be stress relieved. The exception that Section VIII makes is for Grade A (1/2% chromium – 1/2% molybdenum) pressure vessels less than 0.58 in. thick in which case no stress relief is required.

It is common practice to stress relieve the pressure vessels in Group I of SA-387 (Grades A, B and C) by heating them slowly to a temperature range of 1150 to 1300° F, holding 1 hr per inch of thickness at temperature, and cooling slowly in a still air atmosphere to a temperature not exceeding 600° F. The same general practice is followed for stress relieving pressure vessels in Group II of SA-387 (Grades D and E) and SA-357, except the temperature range for stress relieving may be 1200 to 1350° F.

7. Heat Treatment of Nickel-Steel Pressure Vessels

Heat Treatment of Low-Nickel-Steel Pressure Vessels—ASME SA-203

Specification SA-203 covers flange and firebox quality plates of 2.00 to 2.75% and 3.25 to 3.75% nickel steels. The various grades have the following general chemical composition:

Grade	*Carbon, max., %*	*Nickel, %*
A	0.17	2.5
B	0.20	2.5
C	0.25	2.5
D	0.17	3.5
E	0.20	3.5

SA-203 plates may be purchased in the hot-rolled or normalized or normalized and stress-relieved condition depending upon the customer's requirements. Plates over 2 in. in thickness shall be treated to produce grain refinement either by normalizing or heating uniformly for hot work.

The majority of pressure vessels made of plate of this specification are of Grade D material in the normalized condition and stress relieved after fabrication.

The general information outlined in Section 2 of this report on heat-treatment procedures after cold work or hot forming of plates and heads applies equally well to nickel-steel pressure vessels except that some fabricators require a stress-relief heat treatment after normalizing of Grades B, C and E.

The maximum temperature for stress-relief heat treatment should not exceed 1175° F as nickel lowers the critical temperature range.

Heat Treatment of High-Nickel-Steel Pressure Vessels—ASME SA-353

Specification SA-353 covers one grade of 8.50–9.50% nickel-steel plate of firebox quality.

The plates before being fabricated shall be uniformly heat treated to produce grain refinement. This is usually accomplished by a single normalizing consisting of heating to 1600° F, holding at temperature for a minimum of 1 hr per inch of thickness but in no case less than 1 hr, and cooling in air; or by double normalizing at 1650 and 1450° F.

Cold Forming High-Nickel-Steel Plate

High-nickel steel in the as-rolled or normalized condition has a tightly-adhering highly-refractory scale which should be removed by pickling or sand-blasting. The scale formed by stress-relieving treatments is not sufficiently heavy to affect fabricating operations.

Before cold forming, SA-353 plate should be stress relieved by heating to 1025 to 1085° F, holding at temperature for a minimum of 2 hr for thicknesses up to 1 in. plus 1 hr for each additional inch of thickness, and cooled in still air to a temperature not exceeding 600° F. The maximum thickness permissible for cold forming is dependent on the radius to which the plate or head is to be bent but generally plate of SA-353 over 1 in. is hot formed.

If the outer fiber stretch of the plate exceeds 3%, the cold-forming operation is generally done in two or more stages with the application of an intermediate heat treatment of 1025 to 1085° F.

Final Heat Treatment of High-Nickel-Steel Pressure Vessels

The final heat treatment of SA-353 pressure vessels shall consist of heating uniformly to a temperature between 1025 and 1085° F, holding for a minimum of 2 hr for thicknesses up to 1 in., plus a minimum of 1 hr for each additional inch of thickness, and cooling in still atmosphere to a temperature not exceeding 600° F.

8. Heat Treatment of Quenched-and-Tempered Low-Alloy-Steel Pressure Vessels

Case No. 1204 was approved by the ASME Council on Nov. 15, 1955. This Case gave permission to use quenched - and - tempered alloy - steel plates of firebox quality and alloy-steel forgings in the construction of welded pressure vessels under the rules of Section VIII of the Code with several additional mandatory requirements. Among these mandatory requirements were the following:

1. The maximum thickness of shell and head plates shall be 2 in.
2. The maximum operating temperature shall not exceed 650° F.
3. The requirements of Paragraph UG-84 shall be met for vessels that are to operate at temperatures below −20° F.
4. Chemical composition:

	%
Carbon	0.10–0.20
Manganese	0.60–1.00
Phosphorus, max	0.035
Sulfur, max	0.040
Silicon	0.15–0.35
Nickel	0.70–1.00
Chromium	0.40–0.80
Molybdenum	0.40–0.60
Vanadium	0.03–0.10
Copper	0.15–0.50
Boron	0.002–0.006

5. Heat treatment: The material shall be treated by the material manufacturer to produce the following tensile requirements by heating to not less than 1650° F, quenching in water between 50 and 100° F, and tempering to not less than 1150° F with a holding time of 1 hr per inch of thickness, but in no case less than $^1/_2$ hr.

Tensile strength, psi, min	105,000
Yield strength, psi, min	90,000
Elongation in 2 in., min, %	17

6. Shell plates shall not be heated for forming to a temperature higher than 1100° F. Other parts, including heads, that are heated for forming to temperatures over 1100° F shall be heat treated by the manufacturer of the head or part. Forgings shall be heat treated by the manufacturer of the forging. Test specimens prepared from full thickness material in its final heat treated condition, or from full thickness samples similarly and simultaneously treated, shall be taken from any pressure parts that have been heat-treated above 1100° F after forming.

7. When the shell or head thickness exceeds 0.58 in., the finished vessel shall be stress relieved as provided in Paragraph UCS-56(c). The stress relieving temperature shall be between 1000 and 1100° F (at least 50° F below the minimum tempering temperature).

Cautionary Note: Filler metal deposited by nickel-molybdenum-vanadium type of electrodes can become embrittled at the above stress relieving temperature.

The points discussed above only pertain to that portion of Case No. 1204 regarding mandatory heat treatment and fabrication requirements. Many other requirements that must be met in the construction of welded pressure vessels are specified in this Code Case, but these requirements are beyond the scope of this report. The high tensile properties specified for this type of low-alloy steel are obtained by water quenching from a temperature of not less than 1650° F with a mandatory minimum tempering temperature of 1150° F.

9. General Metallurgical Considerations in the Heat Treatment of Carbon- and Low-Alloy-Steel Pressure Vessels

Stout[1] has ably summarized the effects of preheating and postheating of pressure-vessel steels with particular emphasis on the soundness and mechanical properties of welds. He also includes a table containing recommended preheating and postheating temperatures for various carbon contents and thicknesses of the grades of steel commonly used for pressure vessels except for chromium-molybdenum steels. Stout discussed the purpose and effects of preheating and postheating. He stated that preheating is used by the welding engineer because it helps:

1. To prevent the formation of cracks in the weld metal or in the heated base metal.
2. To reduce distortion of the weldment.
3. To avoid loss of ductility and toughness in the weld and base metal.

These practical benefits are obtained from preheating by the following actions: (*a*) the thermal gradient is reduced; (*b*) the cooling rate is reduced.

Figure 6 illustrates the submerged-arc welding of a head seam on a large drum, with preheat torches removed from beneath the head so that the picture could be taken.

Stout stated that postheating is used to produce two principal beneficial effects:

1. Residual stresses can be relieved to a low level.
2. The heat-affected base metal is softened and toughened by the metallurgical changes, and any dissolved hydrogen present is given an added opportunity to escape.

The authors suggest some additional benefits of postheating or stress relieving:

1. It reduces the hardness of the base metal after hot forming or heat treating above the upper critical temperature.
2. It reduces the hardness of the weld metal thus giving added toughness to the weldment.

The reduction of the hardness of the weld metal and heat-affected zones in the base metal is more important for some of the low-alloy steels than for carbon steels and is the primary reason for postheating or stress relieving some of the low-alloy steels at the higher temperature ranges.

The reports prepared by Stout and his associates,[1–6] under the sponsorship of the Materials and Fabrication Divisions of the Pressure Vessel Research Committee, represent the most complete compilation of data for carbon-steel pressure vessels that the authors could find in the literature survey made for the preparation of this report. Some of their conclusions are listed below in the following summary:

1. Two heats of steel were studied—(1) ASTM A-70 or A-285 rimmed

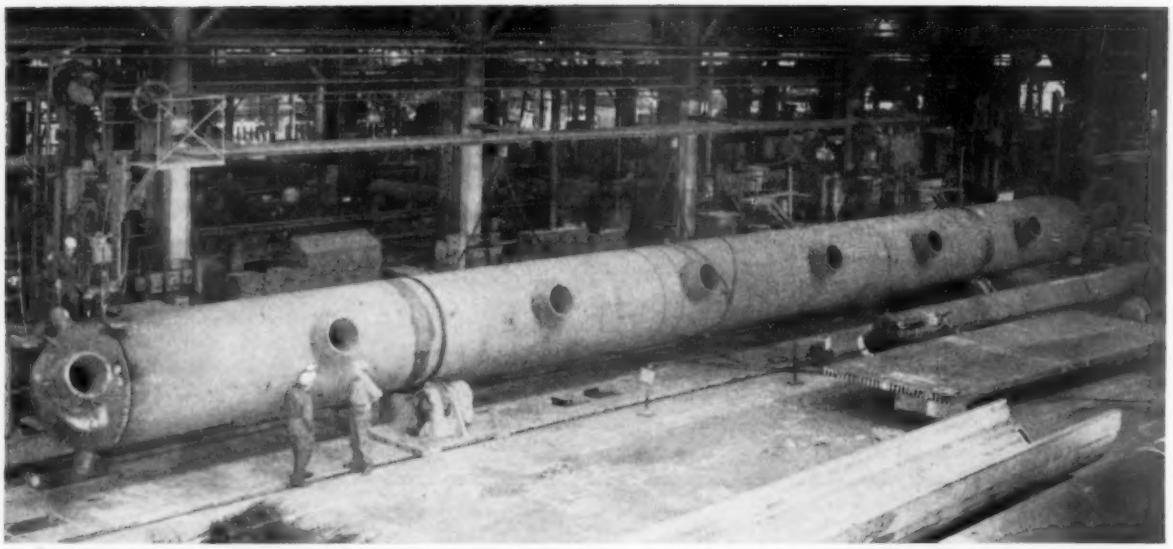

Fig. 6 Submerged-arc welding of a head seam on SA-302 Grade B drum that is 66 in. ID by $4^{7}/_{8}$ in. thick by 101 ft long Shipping weight of drum, 234 tons. (Preheat torches removed from beneath head so that picture could be taken)

steel and (2) ASTM A-201 aluminum-killed steel.

2. The notch toughness of the rimmed A-285 steel was inherently lower than that of the aluminum-killed A-201 steel.

3. Prestraining (up to 10%), welding or heat treatment altered the properties of both A-201 and A-285 steels in the same direction and to about the same extent.

4. Edge preparation—Shearing seriously lowered the notch resistance of both A-201 and A-285 steels. Oxygen cutting had a much less severe effect. Postheating, especially normalizing at 1600° F, was effective in restoring the loss of notch toughness induced by shearing or oxygen cutting. Postheating at 1150° F was noticeably beneficial to the steels but not as effective as normalizing treatment. This was an indication that the loss of notch toughness was due to the heat-affected zone in the case of the oxygen-cut edge or to the plastic deformation of the sheared edges and not due to the small notches introduced by these operations. Machined edges consistently showed excellent toughness with low transition temperatures.

5. Cold forming—Cold working did not seriously lower the ductility or notch toughness of these steels. Heating to temperatures up to 800° F after cold working produced considerable loss of notch toughness because of strain-aging effects. The rimmed and killed steels showed equal susceptibility to strain aging. Heating to normalizing temperature of 1600° F was necessary to completely erase the effects of cold working.

6. Welding—Welding reduced notch toughness or raised the transition temperatures of the carbon steels more than cold working and strain aging. Cold-worked specimens were only slightly less notch tough after being welded than were unstrained specimens after welding. Thus the effects of cold working and welding did not appear to be additive. Postheating to 1100° F or above restored much of the loss of notch toughness due to welding.

7. Transition temperature tests on spherically hot- and cold-pressed specimens indicated that cold pressing definitely raised the transition temperature, whereas hot pressing resulted in transition temperatures that were equal to or better than the unpressed rimmed (A-70 or A-285) or aluminum-killed (A-201) steels.

These conclusions were based on laboratory test data and were the results of a five-year program at Lehigh University.

The possibility of drawing definite conclusions about the effects of fabrication operations on service properties from these laboratory tests is hampered by the fact that there are factors which enter into the manufacture of pressure vessels which could not be incorporated into these laboratory tests. However, these tests do provide information about the direction and extent of the effects that cold forming and welding may produce in carbon-steel pressure vessels of A-70 or A-285 rimmed steel and A-201 aluminum-killed steel.

Brittle Fracture

Considerable attention has recently been focused on "brittle fractures," particularly with regard to failures that have occurred in a brittle manner at temperatures above which the material had been considered satisfactory for the intended applications. It was found that some of the steels investigated may have a transition temperature as high as +100° F, depending on various metallurgical factors such as deoxidation practice, composition, rolling practice and subsequent heat treatment. Armstrong[8] and co-authors discussed four conditions that control the tendency for steel to behave in a brittle manner. These conditions include (1) high stress concentrations—i.e., notches, nicks, scratches, internal flaws or sharp changes in geometry; (2) high rate of straining; (3) a low operating temperature; and (4) several metallurgical factors.

The metallurgical factors, including deoxidation practice, composition, rolling practice and subsequent heat treatment, influence the transition temperature of steel which, under the worst conditions, may be above 100° F or, under the best conditions, below −200° F.

The transition from ductile to brittle behavior of a steel is generally expressed in terms of temperature. McGeady[9] reports on carbon-steel pressure vessels in a temperature range from +32 to −50° F, with particular emphasis on transition temperature behavior. He states that on failed pressure vessels, failure has been generally associated with brittleness of the steel at operating temperature at the time of failure. Pellini[10] discusses three critical transition temperatures and the comparative values as determined by the Charpy V-notch test, the crack-starter explosion test and the drop-weight test. He also points out that there are four possible paths along which brittle fracture may propagate. These are: (1) the plate, (2) the heat-affected zone, (3) the fusion line and (4) the weld metal. Propaga-

tion through any given path becomes possible only if the service temperature is below the critical fracture temperature for that zone. Transition temperatures determined by notch tests furnish valuable information for design, but as yet these data are qualitative because test data for only a few steels have been correlated with actual service performance.

The above references[8,9,10] are only a few of many reports and papers that have been written on the subject of "ductile and brittle fractures." It is recognized that accelerated cooling, such as spray quenching or even dip quenching, from above the upper critical temperature followed by stress relieving or tempering heat treatment is beneficial in lowering the temperature at which a material becomes notch sensitive or gives a "brittle" fracture. The practical application of an accelerated cooling treatment, such as spray or dip quenching, to pressure vessels is a problem to many fabricators. *Good design* and *excellent workmanship* are just as important in the life of a pressure vessel as *high notch toughness* in plate materials and weld metals. The ASME Boiler and Pressure Vessel Code Committee has recently appointed a Special Committee on Brittle Fracture to study and correlate the data obtained on this subject with regard to boiler and pressure-vessel components.

High-Temperature Properties

Romer and Newell[11] discuss the significance of the time factor in testing in relationship to long-life high-performance steam-generating equipment as well as the general effects of metal oxidation from steam and combustion atmospheres. The relative merits of long-time creep tests and stress-rupture tests are considered. A résumé is given of field experience on carbon and alloy steels. It was emphasized that the metallurgical stability of steels is important in maintaining creep strength and is necessary to prevent severe modification of mechanical properties through effects such as spheroidization of carbide phase, graphitization or formation of sigma phase in highly alloyed steels. However, the formation of sigma phase is not a problem in the carbon and low-alloy pressure-vessel steels.

Wylie and Thielsch[12] prepared an interpretive engineering statement of the present knowledge on the creep and stress-to-rupture characteristics of pressure-vessel steels. These authors pointed out that, in the design of pressure vessels for service applications at elevated temperatures, various factors may impose limitations on the service life. Thus, such factors as creep and stress-rupture, fatigue, surface oxidation, structural changes within the material, corrosion and others have to be considered in the selection of materials and in the design of pressure vessels. Among these, creep and stress-rupture represent two of the more important factors.

There is some information in the British literature to the effect that temperatures of 1300–1350° F are not beneficial to the long-time characteristics of certain chromium-molybdenum steels. One authority in Great Britain states that, in general, short-time heatings at these temperatures tend to break down the carbide structure which influences the long-time stability of these steels. However, there seems to be general disagreement among the authorities in the United States as to any harmful effects of the 1300–1350° F stress-relieving heat treatment; so, until such time as definite harmful effects are shown to be due to this heat-treating range, many fabricators will continue to use the temperature range of 1300–1350° F for final heat treatment of some of the grades of chromium-molybdenum pressure vessels.

Chromium-Molybdenum Weld Properties

Carpenter and Floyd[13] have presented the latest data on low-carbon (0.05% max) chromium-molybdenum electrodes for welding materials containing from $^1/_2$ to 5% chromium and $^1/_2$ to 1% molybdenum. This paper includes the effect of carbon on chromium-molybdenum welds, coating characteristics of these E-XX15 low-carbon lime-coated chromium-molybdenum electrodes, selection of electrodes, weld groove design and preparation, preheat, welding technique and postweld heat treatment. The room-temperature mechanical properties of these welds in the as-welded condition as well as stress relieved at different temperatures in the range of 1150 to 1350° F have been presented. Data on tensile tests conducted at temperatures of 800 to 1100° F on the various types of low-carbon chromium-molybdenum weld metals in both the as-welded and 1350° F postweld heat-treated conditions are included in the paper. Stress rupture strength of these weld metals from tests conducted at temperatures of 1000 to 1200° F is compared to stress rupture data on wrought materials of similar chemical compositions. Some general data are also presented on 7% chromium – $^1/_2$% molybdenum and 9% chromium – 1% molybdenum weld deposits. Other topics of general interest included in this paper are studies of carbon migration and temper brittleness of low-carbon chromium-molybdenum weld metals.

10. Subjects for Further Investigation and Study

In view of the lack of information in the literature regarding the fabrication and heat treatment of low-alloy plate materials, it is apparent that there are many subjects that should be investigated to give the fabricators essential basic information that is necessary for further progress in the fabrication of pressure vessels. There are also many controversial subjects that have been investigated through the use of small laboratory specimens but have not been proven by shop practices.

Some of the questions that should receive serious consideration in further pressure vessel investigations are the following:

1. May lower stress-relieving temperatures be used for low-alloy-steel pressure-vessels than now employed by most fabricators?

2. Can sufficient data be gathered for the ASME Boiler and Pressure Vessel Code Committee to permit the relaxation of the requirements for Section I pressure vessels to the extent permitted for Section VIII pressure vessels?

3. May the postheat treatment (stress relief) of low-alloy-steel pressure vessels be eliminated in the following plate materials for wall thickness less than 0.58 in.? SA-301 Grade B or SA-387 Grade B (1% Cr – $^1/_2$% Mo); SA-387 Grade C (1$^1/_4$% Cr – $^1/_2$% Mo); and SA-302 (Mn-Mo).

4. What are the effects of the different types of heat treatment on the notch toughness of low-alloy pressure-vessel steels?

5. What are the effects of time and temperature of postheat treatment on the notch toughness of low-alloy pressure-vessel steels?

6. What is the relative importance of residual stresses compared to softening effects on hard microstructures in postheat treatment (stress relief) of low-alloy pressure-vessel steels?

7. What are the maximum limits (maximum outer fiber stretch) that can be safely used in cold forming carbon-steel and low-alloy plates without requiring intermediate heat treatments?

8. What are the effects of postheat treatment (stress relief) at 1350° F on the creep and stress rupture properties of chromium-molybdenum pressure vessels?

A task group was appointed approximately two years ago by the ASME Boiler and Pressure Vessel Code Committee to make recommendations on "Correlation of Stress Relieving, Radiographing and Welding Requirements."

With regard to Questions 1–3, the Subcommittee on Unfired Pressure Vessels has studied the Task Group's recommendation and approved revisions in Paragraph UCS-56 and has proposed a new Table UCS-56 of Section VIII, "Rules for Construction of Unfired Pressure Vessels," on heat treatment require-

ments for carbon and low-alloy steels that relax some of the restrictive requirements on the low-alloy steels that are not classified as air-hardening steels. The main ASME Boiler and Pressure Vessel Code Committee is considering these revisions at the present time. Meanwhile, the Subcommittee on Power Boilers is considering the report of the Task Group with regard to revisions of Section I, "Rules for Construction of Power Boilers."

In consideration of Questions 4, 5 and 8, the authors' company has development programs in progress on these topics. Reference 13 contains some pertinent data on Question 8.

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AN INVESTIGATION OF WELDED OPEN-WEB EXPANDED BEAMS

Pilot investigation of three open-web expanded beams shows good agreement between calculated and measured deflections in the elastic range

BY M. D. ALTFILLISCH, B. R. COOKE AND A. A. TOPRAC

ABSTRACT. This paper presents the results of a pilot investigation of three open-web expanded beams carried out at the Civil Engineering Laboratories of the University of Texas. The three beams were subjected to two concentrated loads and their structural behavior studied both in the elastic and plastic range. An approximate analysis for stress and deflection for such beams is presented. Good agreement is noted between calculated and measured deflections in the elastic range.

Introduction

Open-web expanded beams consist of two halves of rolled-steel shapes joined by welding after the web has been flame cut in a broken (zig-zag) line, thus producing a beam with increased depth and holes in the web. Figure 1 is an example of an open-web expanded beam. The expanded beam has larger section modulus than the original shape without an increase in weight. Consequently, the beam has a larger carrying capacity and is stiffer than the original unexpanded shape. This method of expansion may likewise be used to fabricate wedge beams, as shown in Fig. 2.

In the following report the results of an experimental investigation of three expanded beams are presented. Strength and deflection of these beams, when subjected to two symmetrical concentrated loads, have been observed and the structural behavior is described. An approximate method for the analysis of such beams is also presented.

Review of Previous Applications and Research

The expanded beam does not seem to be a new idea. It has been used in both building and ship construction under various names, such as "castellated" beam, "serrated" channels and angles and others. The idea of splitting and expanding beams to increase their section modulus was first used, however, about 1910, by H. E. Horton of the Chicago Bridge and Iron Works.

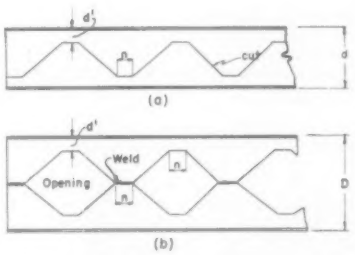

Fig. 1 Fabrication of expanded beam: (a) web cutting pattern; (b) welds and openings in expanded beam

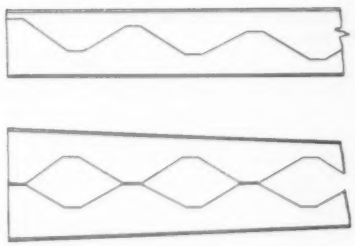

Fig. 2 Expanded wedge beam

Expanded beams have been employed in building construction as floor joists[1] and as arched roof girders of 90-ft span.[2] Their use has been also extended to many industrial and commercial structures.[3–5] Figure 3 shows portions of a 100-ft simple span beam bridge built in 1951 by the Texas State Highway Department. For this project a 36WF160 beam was expanded to a 60-in. depth. In 1952 the same organization built a similar type bridge of a 65-ft span by expanding a 36WF150 to a 48-in. depth. In the shipbuilding industry, on the other hand, the principle of the expanded beam has been used for stiffening the deck and bulkhead plates with "serrated" channels.

Although expanded beams have not yet been used extensively, current applications indicate many additional possibilities for their fabrication and use in the future. A recent cost study made of two deck plate girders—one riveted and the other of the welded expanded type—indicated that the use of the latter resulted in a saving not only of 25.8% weight of steel but also of 34.6% in cost of fabrication, erection and painting.[6] In fact, the limited use of the expanded beam to date has prob-

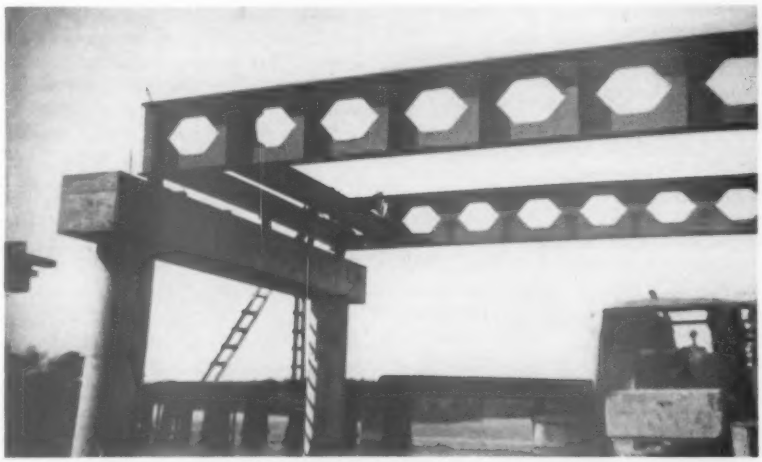

Fig. 3 A 100-ft simple span bridge in Texas

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Presented at the 1956 AWS National Fall Meeting, Cleveland, Ohio, October 8–12.

ably been due largely to the lack of an adequate design procedure.

A literature search for previous investigations of expanded beams in bending revealed that the only load tests were performed in 1943 on "serrated" angles.[7] Tests[8] have likewise been conducted on perforated cover plates subjected to axial loads. This second series of tests does indicate another possible application, but reveals nothing about the possible behavior of open-web beams which are subjected to bending. Although an investigation[9] on the load carrying capacity of such beams has been carried out by the Armed Services, the results of the research have not been made public.

Variables and Objectives of the Investigation

In the expansion of a given rolled shape many variations are possible. For example, the depth of the throat section, d' (Fig. 1), decreases as the depth of the expanded section increases. The length, n, of the throat section, is variable and the same as that of the weld in the solid section of the web. The slope of the diagonal cut may vary also.

For the economical application of this type of construction, the upper limits of expanded depth, the proper slope of diagonal cuts and the throat and weld lengths, together with the stress distribution and deflection characteristics, are of great importance.

The objectives of the investigation reported herein were as follows:

(*a*) To investigate the structural behavior of expanded beams, both in the elastic and plastic range.

(*b*) To determine their strength and mode of failure.

(*c*) To present approximate methods of stress and deflection analysis and compare calculated values to observed strengths and deflections. The data for this report have been obtained from a pilot investigation carried out at the University of Texas,* and has been partially reported elsewhere.[10]

Approximate Analysis of Expanded Beams

Stress Analysis

In the analysis of open-web beams, which have a varying cross section, there are many methods which could be employed. Some of these methods would prove to be lengthy and tedious. Moreover, any type of elastic stress analysis will have its inaccuracies unless allowance is made for the stress-raising tendency of the abrupt changes in the web cross section. The procedure outlined below is an approximation, in that reasonable assumptions are made in order to arrive at a design procedure that is both easy to apply and valid within an allowable percent of error. This analysis is similar in some respects to the simplified method of solution of the "Vierendeel Truss."[11]

A typical example of the open-web beam is provided in Fig. 4(*a*). Here the two cross sections, *A-A* and *B-B* (as shown in detail in Fig. 4(*b*)) are of especial interest. For a portion of such a beam under pure bending moment, flexural stresses are found by making the following assumptions:

(*a*) Flexural stresses are considered uniform through the throat or tee section of the open-web beam (Section *A-A*).

(*b*) Bending moment stress in the solid section is assumed to vary in straight line proportionality from the centerline of the beam; and the f =

* Strain gage data for elastic stress distribution taken during this last-mentioned investigation will be the subject of a second report.

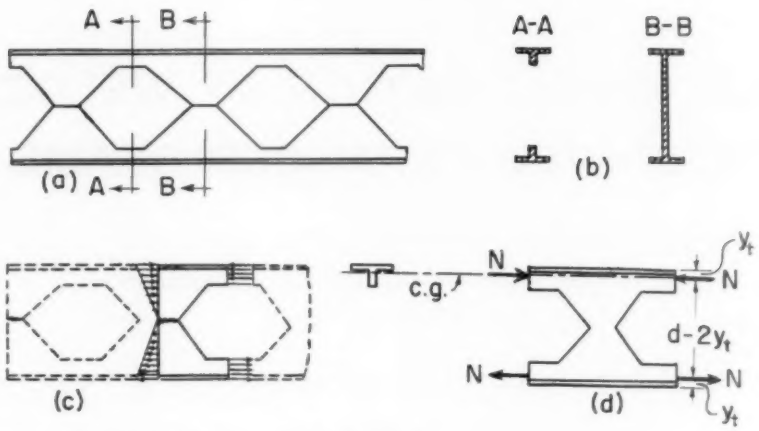


Fig. 4 Distribution of stresses due to bending

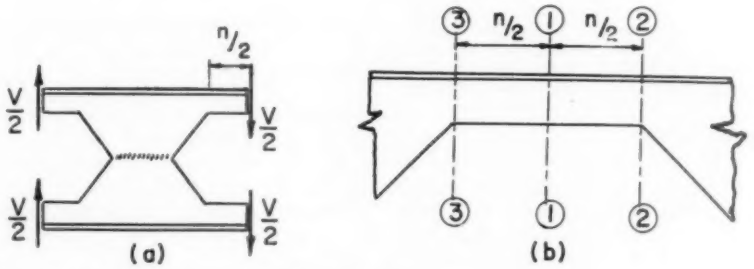


Fig. 5 (*a*) Shear forces; (*b*) critical sections

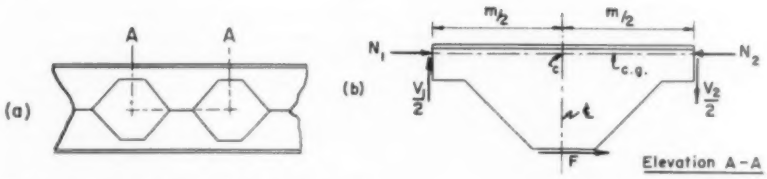


Fig. 6 Shear force acting on web weld

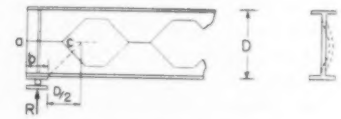


Fig. 7 Vertical buckling

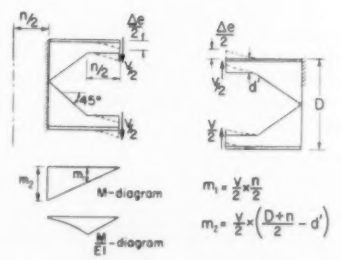


Fig. 8 "Distortion" deflection

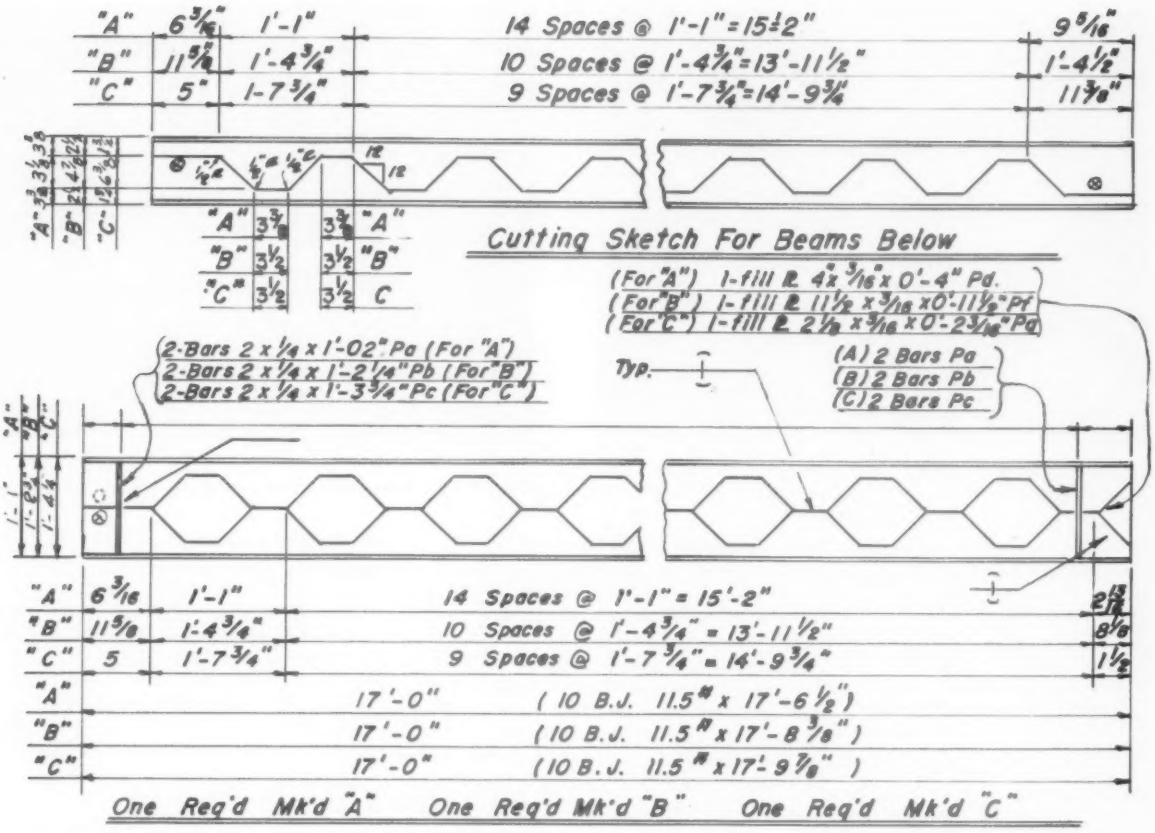

"A" 6 3/16" 1'-1" 14 Spaces @ 1'-1" = 15'-2"
"B" 11 5/8" 1'-4 3/4" 10 Spaces @ 1'-4 3/4" = 13'-11 1/2"
"C" 5" 1'-7 3/4" 9 Spaces @ 1'-7 3/4" = 14'-9 3/4"
9 5/16"
1'-4 1/2"
11 7/8"
Cutting Sketch For Beams Below
(For "A") 1-fill PL 4" x 3/16" x 0'-4" Pd.
(For "B") 1-fill PL 11 1/2" x 3/16" x 0'-11 1/2" Pf
(For "C") 1-fill PL 2 1/8" x 3/16" x 0'-2 3/16" Pq
2-Bars 2 x 1/4 x 1'-02" Pa (For "A")
2-Bars 2 x 1/4 x 1'-2 1/4" Pb (For "B")
2-Bars 2 x 1/4 x 1'-3 3/4" Pc (For "C")
Typ.
(A) 2 Bars Pa
(B) 2 Bars Pb
(C) 2 Bars Pc
"A" 6 3/16 1'-1" 14 Spaces @ 1'-1" = 15'-2"
"B" 11 5/8 1'-4 3/4" 10 Spaces @ 1'-4 3/4" = 13'-11 1/2"
"C" 5 1'-7 3/4" 9 Spaces @ 1'-7 3/4" = 14'-9 3/4"
"A" 17'-0" (10 B.J. 11.5# x 17'-6 1/2")
"B" 17'-0" (10 B.J. 11.5# x 17'-8 3/8")
"C" 17'-0" (10 B.J. 11.5# x 17'-9 7/8")
One Req'd Mk'd "A" One Req'd Mk'd "B" One Req'd Mk'd "C"

Fig. 9 Shop drawing for specimens A, B, and C

Mc/I, with I representing the moment of inertia of the solid section, is assumed to be applicable and satisfactory.

The stress distribution based on the above two assumptions is illustrated in Fig. 4(*c*). Due to the first assumption, the flexural stresses in the throat section give resultant normal forces acting at the centroid of this section, as illustrated in Fig. 4(*d*). If M is the bending moment at the throat section under consideration, then:

$$M = N(d - 2y_t)$$

and the resulting unit stress is

$$f_n = \left(\frac{M}{d - 2y_t}\right)\left(\frac{1}{A_t}\right) \qquad (1)$$

in which

y_t = distance from extreme fiber to centroid of throat section.
A_t = area, throat section.
f_n = normal stress caused by bending.
N = normal resultant force at throat section.

Whenever vertical shear is present, the total shear, V, at a section through the throat, is assumed to be resisted equally by the two tee sections (see Fig. 5(*a*)). It is further assumed that this shear induces a secondary moment at the open-web throat section by acting as a force at an assumed point of inflection which is located at the middle of the throat section (see Fig. 5(*b*), Section *1-1*).

Consequently, in a tee cross section acted on by both bending moment and shear, there is uniform normal stress at the point of inflection; but at a distance $n/2$ from this point, a secondary bending stress is added to the uniform normal stress. Thus, maximum stress occurs at the section in which the open web begins to vary in depth (Fig. 5(*b*), Sections *2-2* and *3-3*). The stress induced by this secondary "shear" moment can be calculated by means of the flexure formula, $f = Mc/I$, as follows:

$$M = \frac{n}{2} \times \frac{V}{2}$$

and

$$f = \frac{nV}{4} \times \frac{y_t}{I_t} \quad \text{or} \quad f = \frac{nV}{4} \times \frac{y_b}{I_t} \qquad (2)$$

in which

n = throat section length.
V = shear at section.
y_t = distance from flange to centroid at throat section.
y_b = distance from inner edge (at the cut) to centroid of throat section.
I_t = moment of inertia of one flange of throat section.

in which

The critical sections in open-web expanded beams are:

(*a*) Under pure bending the only critical section is at that throat section subjected to the largest moment (eq 1 is used for the evaluation of this stress).

(*b*) Where shear and moment occur together, maximum bending stress is calculated by the algebraic sum of eqs 1 and 2.

(*a*) *Shear at Welds.* Consistent

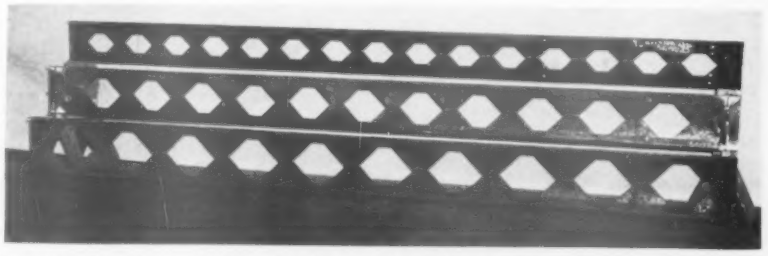


Fig. 10 Specimens A, B and C (top to bottom) as received from the fabricator

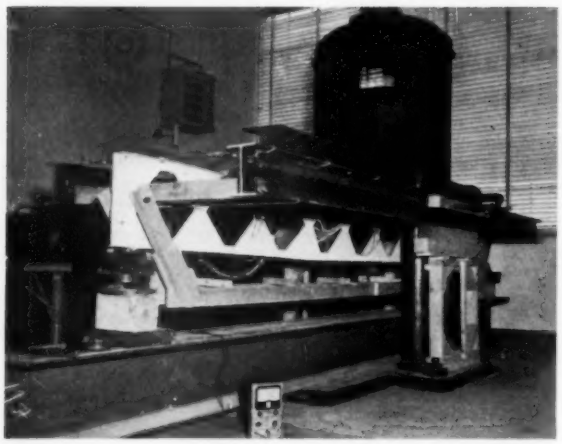


Fig. 11 Picture showing dial supporting wooden yoke. Specimen C

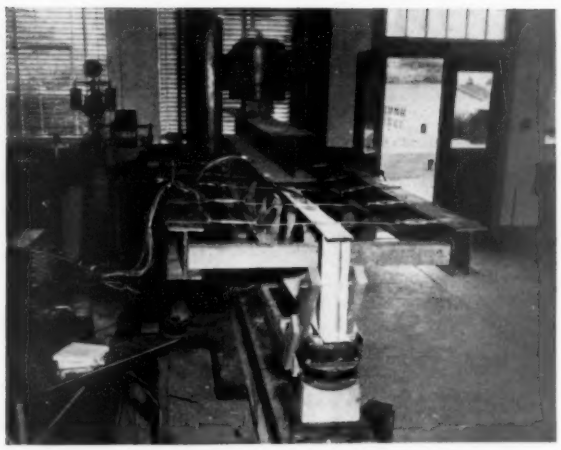


Fig. 12 Picture showing test setup. Specimen C

with assumptions made for bending and shear, a section is taken through the middle of two open-web panels and the weld at the solid section, as illustrated in Fig. 6. Moments about Point C result in an expression for shear at the weld, F, in terms of the shear at the two throat sections on each side of the weld:

$$F\left(\frac{2}{d} = y_t\right) = \frac{m}{2}\left(\frac{V_1}{2} + \frac{V_2}{2}\right)$$

and

$$F = \frac{m}{2}\left(\frac{V_1}{2} + \frac{V_2}{2}\right) \div \left(\frac{d}{2} - y_t\right) \quad (3)$$

The average shear stress, v, then is equal to F/A_w, in which A_w is the least shear area of web per panel.

(*b*) *Crippling and Vertical Buckling of Web.* There is no difference in web crippling stress evaluation between expanded and unexpanded sections. Calculation of this stress, f_c, can be made exactly as in ordinary beams[12] by the formula $f_c = R/[(b + k)t]$, where k is the distance from outer face of flange to web tow of fillet.

Consideration of vertical buckling is necessary for the expanded beams because of the greater ratios of depth to thickness of web. It is assumed here that the critical section in vertical buckling is at mid-height and that the compressive stress is distributed over the length of web, ac, as shown in Fig. 7.

Assuming that the length, ac, is *less* than $[(D/2) + b]$, then $f_c = R/t(ac) = R/A_w$, in which A_w is the web area of length ac. If, on the other hand, ac is *larger*, then $f_c = R/\{t[D/2) + b]\}$. The stress f_c is limited to that allowable for a column having length equal to clear height of the web and thickness equal to that of the web.

Most of the time, even though the actual stress f_c is within that allowed by specifications, stiffeners should be provided. The stiffeners can be designed in the same way as those for welded plate girders.[13]

Deflection Analysis

An evaluation of deflections is as important to the expanded as to the unexpanded beam. The deflections for such beams are to be computed by use of an empirical rule which states that "they are the sum of simple beam deflections increased by a ratio to take into account the increase in stresses opposite panel opening (tee section) and distortion deflections due to secondary moments caused by shear." The steps in computing deflections are as follows:

(*a*) Compute deflection as if the beam were solid throughout (Moment of Inertia of Solid Section).

(*b*) Increase the above deflection by a ratio of: the average of the throat section stress and the extreme fiber stress of the "solid" section to that of the extreme fiber stress of the solid section at maximum moment, $\gamma = (f + f_n)/2 \div f = (1/2) + (f_n/2f)$ in which f = extreme fiber stress in solid section and f_n = extreme fiber stress in throat section.

(*c*) Add shear "distortion" deflections. This additional deflection is induced by the vertical shear forces which

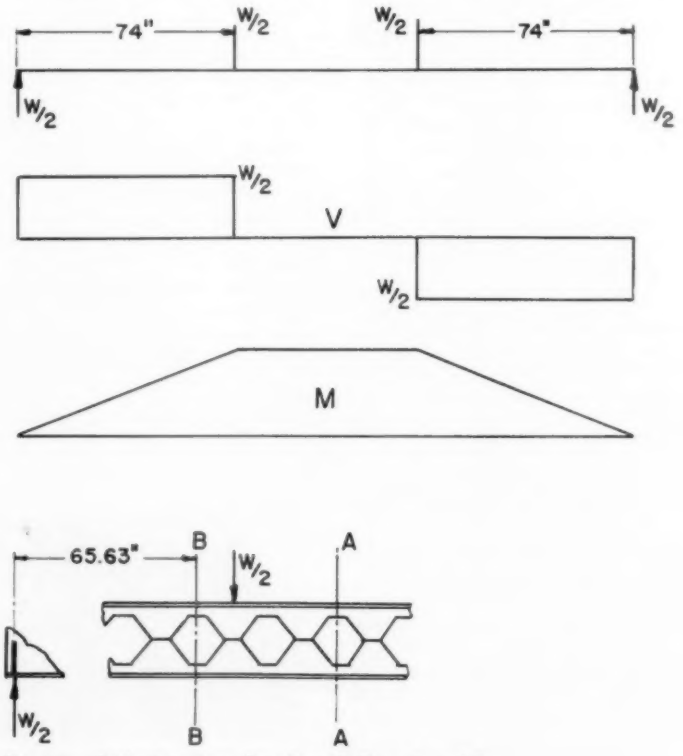


Fig. 13 Critical sections, Specimen B, Tests 1 and 2

Section B-B

Point	f_n	f_b	Stress f_n+f_b	W_Y	Description
2	-1.970 W	+ 1.420 W	-0.550 W		
3	- 1.970 W	- 1.420 W	- 3.390 W	14,000#	First yield, web
4	- 1.970 W	-0.378 W	- 2.348 W	18,380#	Yield in flange
5	- 1.970 W	+0.378 W	- 1.592 W		

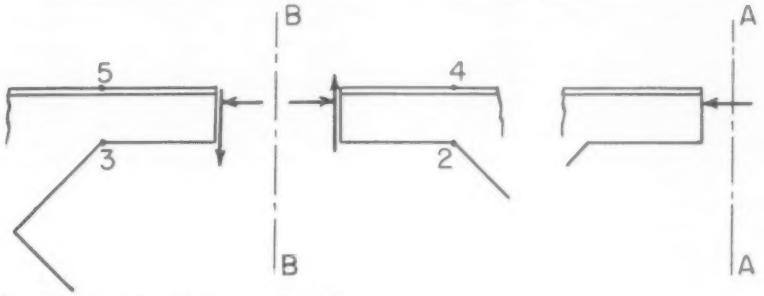


Fig. 14 Specimen B, stresses in Section B–B

distort each panel in the beam. The deflection is not "true" shear deflection but can be considered as a "distortion deflection." Let distortion deflection be equal to Δe per panel. In Fig. 8 the dashed lines represent deflected position (distortion) of panel. Next·

1. Assume open-web section fixed at $n/2$ ft from centerline of solid section.
2. Compute deflection of cut ends $(\Delta e/2)$ using M/EI diagram of a fixed cantilever beam.

Deflection $\Delta e/2$ can be found by means of the Moment-Area Principle (see Fig. 8). The final deflection at any point, then, is that deflection which is computed using the solid section moment of inertia increased by the ratio, γ, and the summation of the shear distortion deflections. Thus:

$$\Delta_{total} = (\Delta_{solid})\gamma + \Sigma\Delta e$$

Specimens and Tests

Load tests[10] were performed at the University of Texas, Department of Civil Engineering, on three test specimens, fabricated from a 60-ft long 10B11.5 joist. The specimens, which were 17 ft 0 in. long, were fabricated and donated to the project by Bethlehem Steel Co. of Beaumont, Tex. After expansion, final depths of the specimens were respectively $1^1/_2$, $1^1/_2$ and $1^2/_3$ times the depth of the original 10B11.5 joist. The angle of the diagonal cut was 45 deg for all specimens and full stiffeners were placed at end reaction points in all specimens. Figure 9, which is a shop drawing, gives the dimensions and details for the specimen. Figure 10 is a photograph of the three specimens as they were received from the fabricator. Table 1 gives the final depths for the three specimens, namely A, B and C.

Only Specimen B was tested without stiffeners at the load points, while for Specimen C short stiffeners $3^1/_2$ in. long, were placed just below the top flange.

Table 1

Specimen	*Expanded depth**
A	$4/3d$ = 13.00 in.
B	$3/2d$ = 14.75 in.
C	$5/3d$ = 16.25 in.

* See Appendix A for section properties and dimensions.

For specimen A full-depth stiffeners were placed at load points.

Table 2 shows the tests performed on these three specimens, the instrumentation and the loading employed. The two equal loads applied symmetrically from the center of the beam resulted in a region of varying moment with constant shear and a region of constant moment with zero shear. Electrical SR-4 strain gages were used to determine strain at various places on the beam. Deflections were measured with $^1/_{1000}$ in. Ames deflection dials. Loading was applied by a 400,000 lb capacity Riehle screw-type testing machine. The span center to center of bearings for all tests in this program was 16 ft 6 in.

Tests to determine the yield point of the metal in tension were made on coupons taken from Specimen B. These coupons were all cut from strips of metal that, in their original positions, were parallel to the longitudinal axis of the beam. The average yield point of two coupons taken from the top flange was 43,150 psi. The yield point of one coupon taken from the web near the flange was 47,400 psi.

Test Setup and Instrumentation

The following is a discussion of the preparation for testing, the lateral bracing used and the instrumentation and test procedure followed.

Because the span of the test specimens was greater than the length of the table of the testing machine, a base beam, 14BP73, was used to transfer the reactions from the test specimen to the machine table. The two-point loading

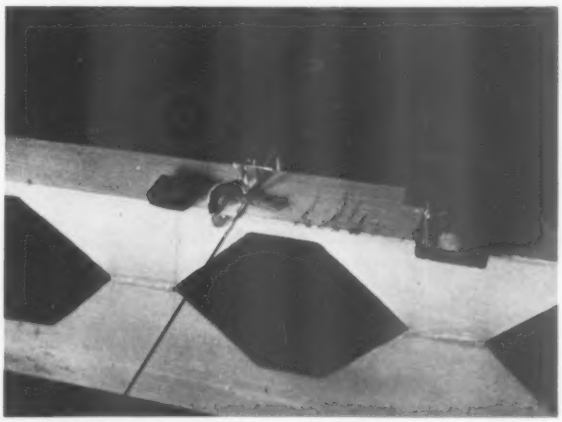
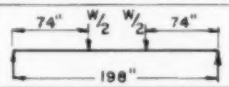
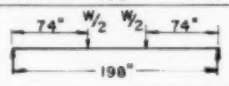
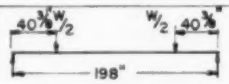
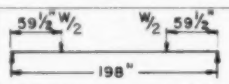
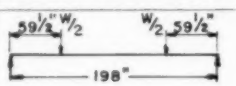
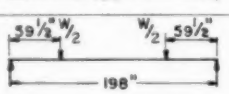
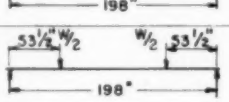


Fig. 15 Specimen B, Test 1. Yield lines on compression flange near load point. Load on specimen—17,670 lb

Fig. 16 Specimen B, end of Test 1. Yield lines on both sides of load point. Pure moment region to the right

Table 2 Expanded steel beam tests

Specimen	Test	Depth inches	Span and Loading	Gages
B	1	14 3/4		7 Ames dials
	2	14 3/4		7 Ames dials 5 SR-4 Type A-11
	3*	14 3/4		5 SR-4 Type A-11 1 Ames dial 3 Clip gages 1-45° Rosette
C	1	16 1/4		34 SR-4 Type A-11 7 Ames dials
	2	16 1/4		34 SR-4 Type A-11 7 Ames dials
	3*	16 1/4		SR-4 Type A-11 1 Ames dial
A	1*	13		7 SR-4 Type A-11 2 Ames dials

* Loaded to failure

was accomplished by means of simple span loading beam which, in turn, was loaded at its center.

To reduce any tendency to buckle laterally (large Ld/bt ratio) the specimens were laterally braced at the compression flange by means of horizontal tension braces. The tension in the braces was adjustable by the use of turnbuckles. At one end the braces were attached to two 10-ft anchor beams which were clamped to each side of the testing machine. At their other end the braces were hooked around the compression flange of the test specimen and clamped in place by small C-clamps. Six such braces were used on each side of the specimen.

Ames deflection dials were used to measure deflections. These dials were supported by a yoke suspended from pins at the neutral axis level over the reaction points of the specimens. The wooden yoke is shown below the beam in Fig. 11. Electrical strain gages were mounted to study the strains, both in the region of pure bending and in that of varying moment and constant shear. The positioning of the gages was varied and new gages were added as each test revealed positions of interest along the beam. All specimens were whitewashed (with slaked lime) to aid in the detection of yield lines. Loads were applied in increments by use of the 400,000 lb capacity testing machine. After each increment was added, strain gage and deflection dial readings were recorded. Figure 12 is a picture of the completed test setup.

Test Results and Discussion

Data regarding the test results are given and discussed below. Test Specimen B is discussed first and Specimen A last. This sequence is the same as that used during testing in this program.

Specimen B

The critical sections in this specimen, which had a height of $1^1/_2$ times the depth of the original rolled shape, are analyzed by means of the procedure outlined earlier. The loads for initial yield are calculated below for Tests 1 and 2 of this specimen (see Table 2).

The sections to be examined for this purpose are: (*a*) any throat section in the constant moment region, and (*b*) the throat section with the largest moment in the region of varying moment and constant shear. The two sections, *A-A* and *B-B*, are those shown in Fig. 13.

In the constant moment region the stress in a throat section is calculated in terms of the total load on the specimen, W, by means of eq 1, as follows:

$$f_a = \frac{M}{d - 2y_t} \times \frac{1}{A_t} = 2.221W$$

In the throat section nearest the load point in the shear and bending moment region, stress in the compression flange and web are computed by use of eqs 1 and 2. The resulting values are shown in tabulated form in Fig. 14. It is seen, therefore, that Point 3 is subjected to the largest (computed) stress for any given load system as applied in Tests 1 and 2.

The calculated load, W, based on a yield point of 47,000 psi, is: $3.390W$ = 47,400 and W = 47,400/3.390 = 14,000 lb. Further examination of the computed results in Fig. 14 shows that the flanges at Point 4 will yield at a load of 18,380 lb.

(*a*) *Tests 1 and 2.* These tests were carried out to the load at which initial yielding occurred in order to check the validity of the approximate analysis for stress and deflection. Results of Test 1 were used as a guide for location of SR-4 electric gages used in Test 2. In both tests, the specimen was loaded symmetrically with two concentrated loads spaced 50-in. apart (see Table 2). On a 198-in. span loads at such spacing emphasized moment stresses rather than shear in the web. Loads were applied to the specimen in increments of 500 lb to maximum values of 17,670 lb in Test 1 and of 17,600 lb in Test 2. At these loads yield lines developed in the compression flange and the test was stopped.

While Test 1 was being carried out, the center deflection of the specimen

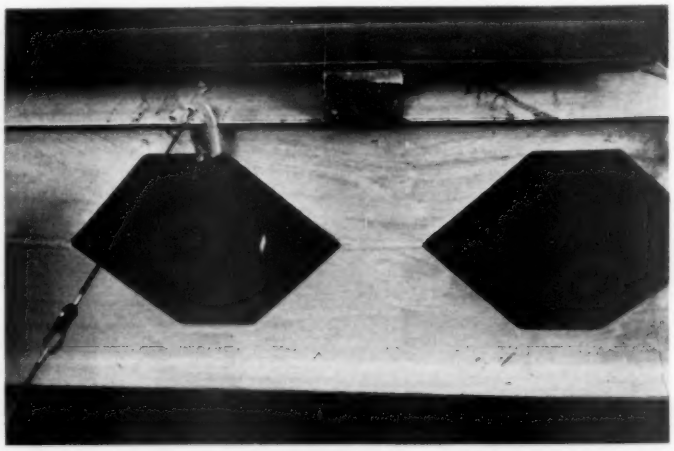


Fig. 17 Specimen B, Test 2, yield lines at end of test. W = 17,600 lb

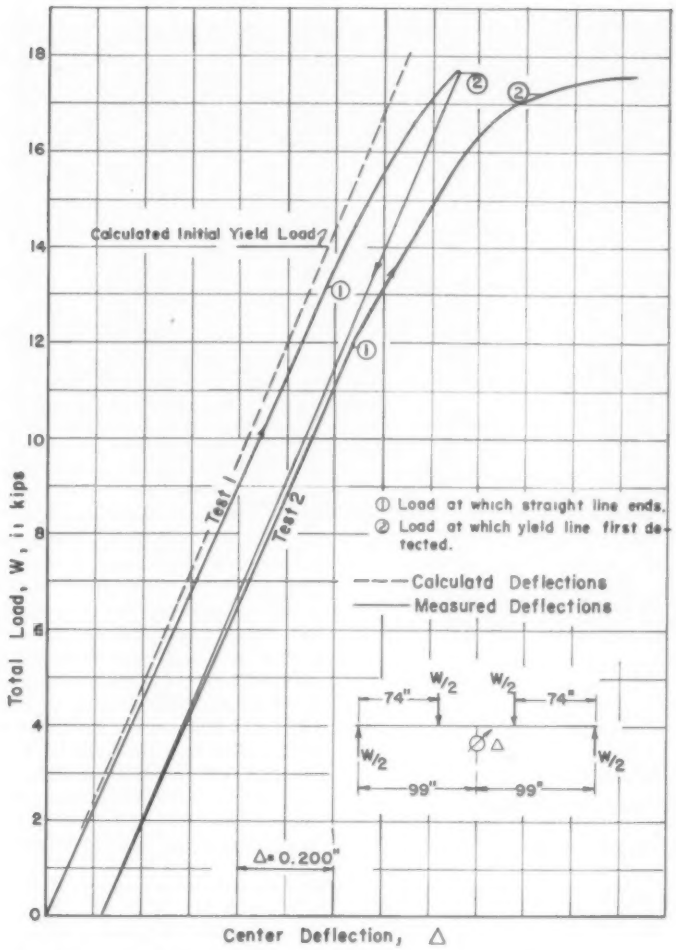


Fig. 18 Load-deflection curve, Specimen B, Tests 1 and 2

was plotted versus load. This curve started deviating from a straight line after one of the lateral braces failed at about 13,150 lb; however, no yield lines were evidenced at this time. At 17,670 lb yield lines began to appear rather rapidly. These lines, which can be seen in Fig. 15, occurred in the compression flange just outside the load point in the region of bending and shear. As this load was kept on the specimen, yielding progressed into the web and also started in the flange of the constant moment section (Fig. 16).

The manner in which yield lines appeared pointed out that the shallow tee section, where maximum moment combines with shear, was a critical area. However, web and welds did not show any evidence of yielding.

In Test 2 the lateral bracing system was strengthened by changing the anchor beams and increasing the number of braces from six to eleven. The test specimen was turned upside down from its position in the first test; thus removing overstressed sections from the compression flange and placing them on the tension side, which was not critical.

In this test some yield lines were first noticed at 17,200 lb total load. These lines occurred in the compression flange near the points of load application, first in the moment and shear region and next in the pure moment region. The load was carried to a maximum value of 17,600 lb and the test was stopped. As indicated in Fig. 17, not only the compression side but also the tension side developed yield lines. A few yield lines in the top flanges penetrated the web for a short distance; the top flange bracing proved to be satisfactory and there was no buckling evidenced.

Figure 18 is a Load-Deflection Curve for Tests 1 and 2. It is of interest to note the close agreement between calculated and measured deflections. Deflections were computed according to the procedure outlined above. Appendix B gives sample computations for the calculation of deflections.

(*b*) *Test 3.* In this test Specimen B was carried to failure. The loads were placed symmetrically about the centerline, 117 $^1/_4$ in. apart, as compared to the 50-in. which was used in Tests 1 and 2. This spacing of load emphasized shear in the web. The tentative objectives of the test were the development of high shear near the ends of the specimen and observation of the manner of failure of the specimen.

The specimen was loaded in increments to 26,000 lb before the first yield line was observed. This line occurred in the compression flange at the suspected

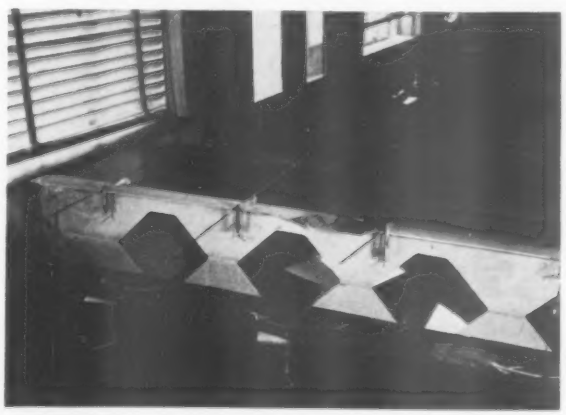


Fig. 19 View showing area of failure and condition of lateral support braces

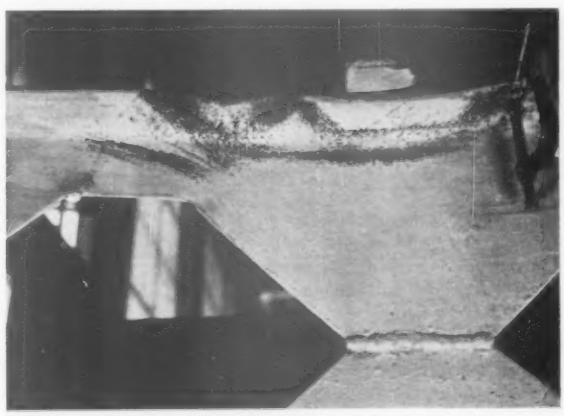


Fig 20 Close-up showing area of failure

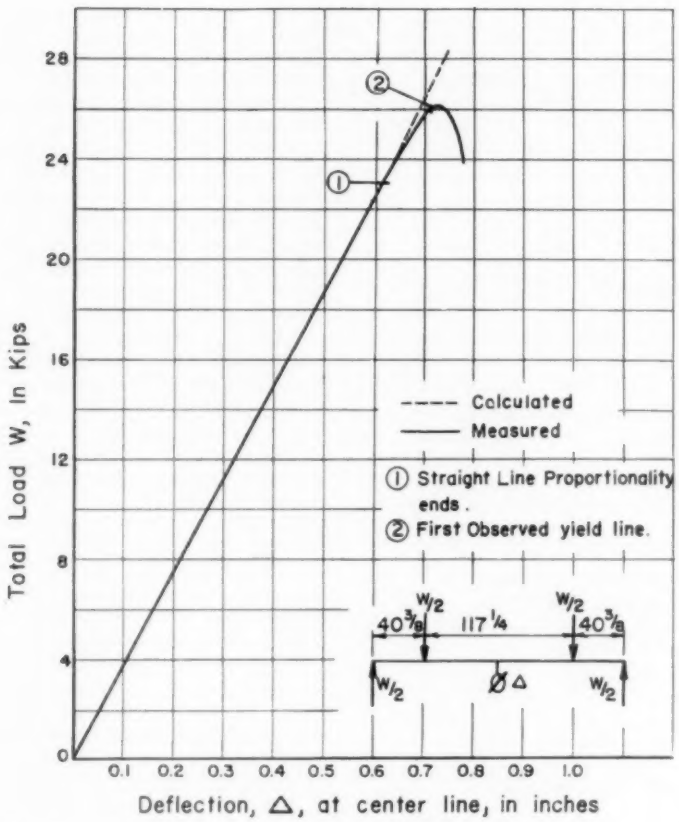
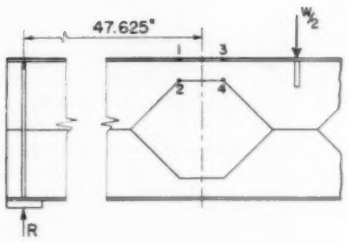


Fig. 21 Load-deflection curve, Specimen B, Test 3



Critical Stresses

Point	f_t	f_b	Stress = $f_t + f_b$	W_{Yield}
1	-1.482 W	+0.657 W	-0.825 W	52,400#
2	-1.482 W	-2.880 W	-4.302 W	11,000#
3	-1.482 W	-0.657 W	-2.139 W	20,200#
4	-1.482 W	+2.880 W	+1.398 W	33,800#

Fig. 22 Computed stresses, Specimen C

In the first two tests, loads were either below initial yield or close to it. In the testing of Specimen C the same system of loading was used for all three tests. Two equal concentrated loads were spaced 79 in. apart symmetrically from the center line (see Table 2). As a result the loads were 59½ in. away from the beam supports. This position of loads put three panels between the support and load while four panels remained between the loads in the constant moment region.

The computed stresses in terms of region, that is, in the shear region just outside of the load point. Immediately after the formation of this yield line the load fell to 25,800 lb. When the loads were increased to 26,500 lb the yield lines penetrated the web and the compression flange buckled. The specimen could not sustain this load and equilibrium was reached at 26,150 lb, which is to be regarded as the maximum. Figure 19 shows the area of failure as well as the inadequacy of the lateral bracing system.

Considerable yielding and flange buckling also took place under the load (Fig. 20). This local effect could have been avoided if bearing stiffeners had been used under the loads. However, the local failure did not influence appreciably the over-all capacity of the specimen subjected to the type of loadings used.

Figure 21 is the load-deflection curve for this test. As can be seen in the figure, there is very close agreement between measured and calculated deflections.

Specimen C

This specimen, expanded to 1⅔ of the original depth, is the deepest of the three open-web beams tested in this program. Three tests, labeled as Tests 1, 2 and 3, were made on this specimen.

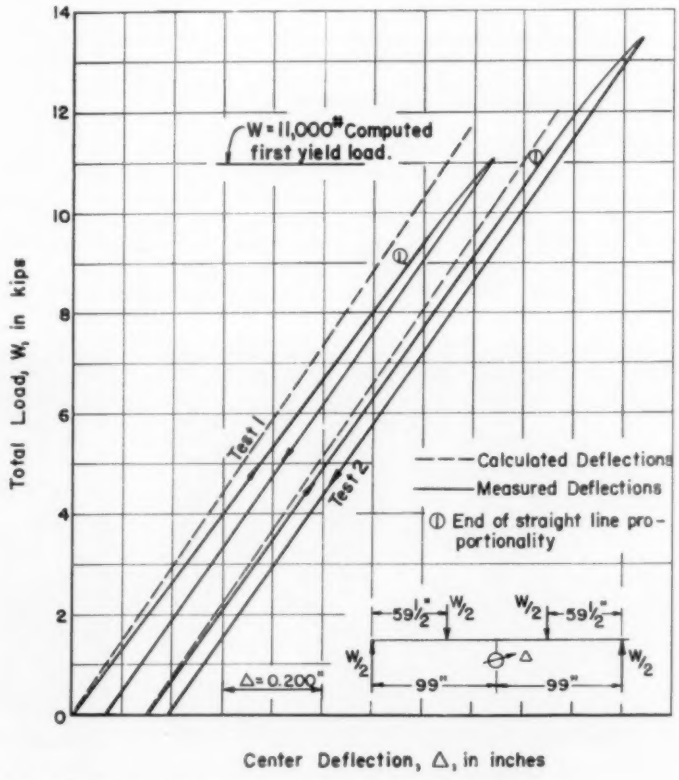


Fig. 23 Load-deflection curve, Specimen C, Tests 1 and 2

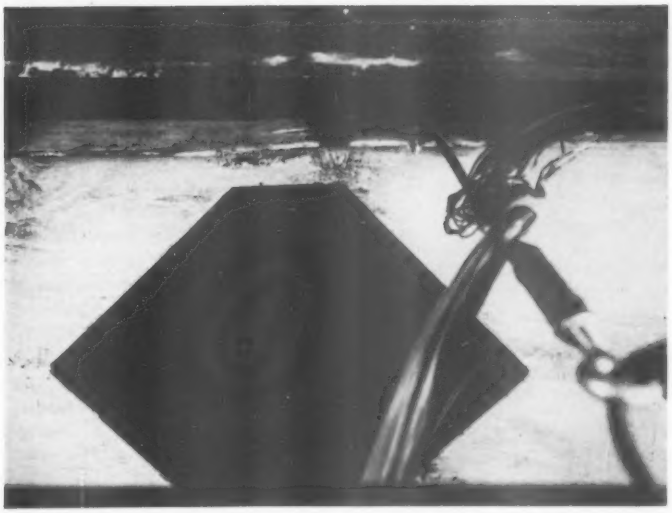


Fig. 24 View after failure of Specimen C, at $W = 1{,}9150$ lb

total load W for the compression web and flange are given in Fig. 22. As in Specimen B, the critical section is that which has the largest moment with shear. The initial yield was computed to occur at Point 2 in the web, as shown in the sketch of Fig. 22, at a load of 11,000 lb. At Point 4, tension stress was predicted. At Point 3, the largest extreme flange stress was calculated.

Because the lateral support system proved inadequate during the testing of Specimen B, it was decided to increase the size of the lateral braces to $^1/_4$ in. and to use heavy turnbuckles in lieu of the screen door braces. In addition, 4-x 4-in. timber posts were inserted as spacers between the webs of the two anchor beams. One such timber spacer was placed through each end-panel opening of the test specimen and secured in place. Figures 11 and 12 show the revised lateral bracing system.

To avoid local effects under the load points use was made of short stiffeners which extended from the top flange a distance of $3^1/_2$ in. down the web.

(*a*) *Test 1.* Here the specimen was loaded by increments to a total load of 11,000 lb, at which point one of the SR-4 electrical strain gages indicated a strain of 1410 microinches per inch. During the removal of this load readings for deflection were taken at four different intervals. No yielding lines were observed but the deflection curve deviated from a straight line at a load of about 9000 lb. In Fig. 23, giving the load-deflection curve for the test, the calculated deflection is also shown and compared to that of the measured. Accordingly, it can be seen that, at the elastic region, the difference between calculated and measured deflection is only 11%.

(*b*) *Test 2.* In this test the specimen was loaded to 13,500 lb. The resulting load-deflection curve is shown in Fig. 23. There is a close agreement in this test between calculated and measured deflections. No yield line was observed.

(*c*) *Test 3.* Yielding was first observed at a load of 15,000 lb during this test to failure of Specimen C. The yield line occurred in the compression web at the place where first yield was predicted (see Fig. 22). As the load was increased, yield lines extended and yielding was more pronounced. Failure took place at the compression flange, which buckled locally just outside the load point in the region of shear and varying moment. The maximum load carried by the specimen was 19,150 lb. Figure 24 shows a view of Specimen C after failure. In this figure it is of particular interest to note the distortion and failure of the shallow tee section. Finally, the load-deflection curve for this test is given in Fig. 25.

Specimen A

Since the amount of expanding in this beam was small, amounting to only $1^1/_2$ of the original depth, it was decided not to use the assumption that stresses induced by moments act uniformly across throat sections. Instead, the stress was assumed to conform to

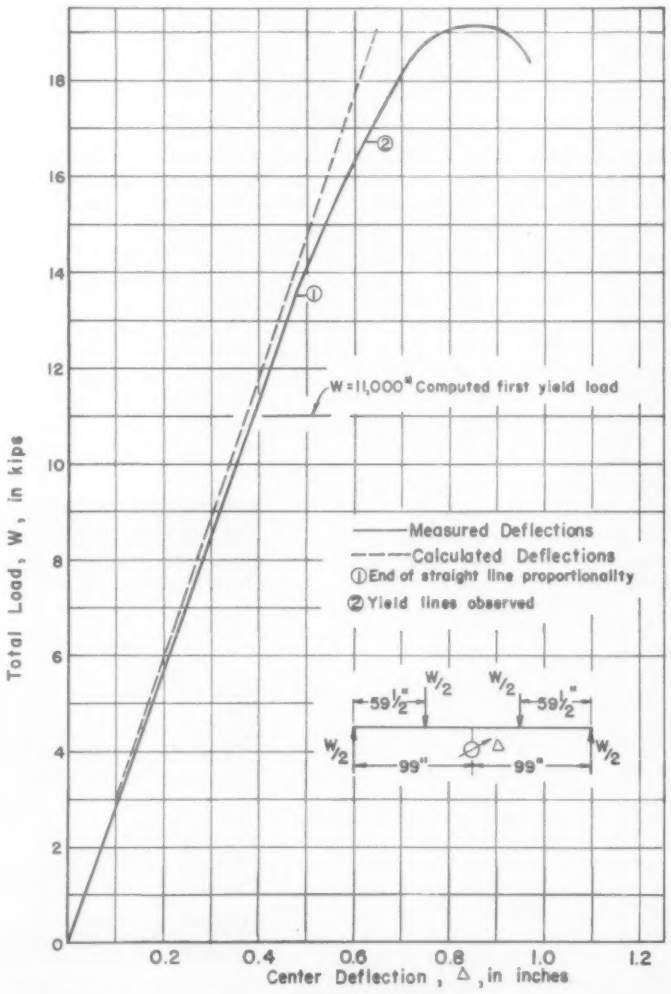


Fig. 25 Load-deflection curve, Specimen C, Test 3

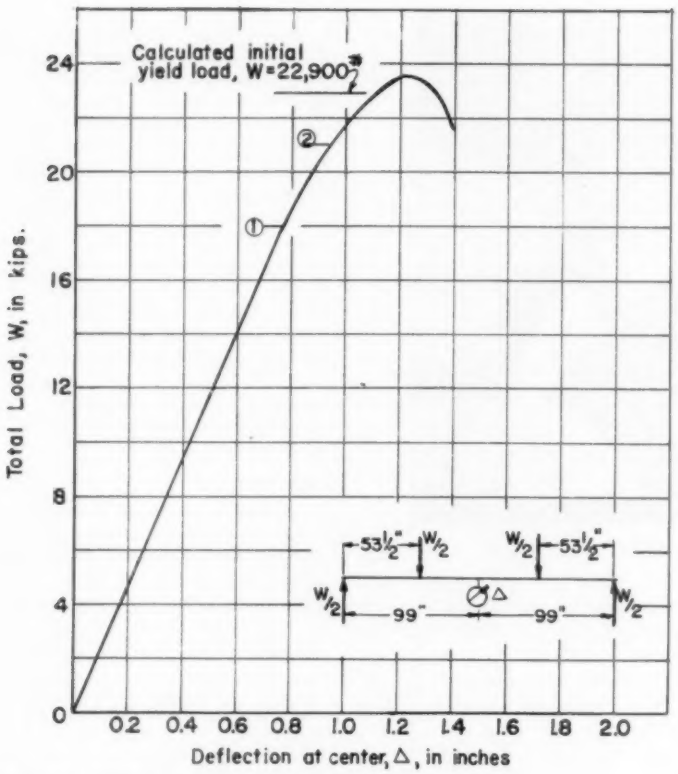


Fig. 26 Load-deflection curve, Specimen A

the flexure formula, Mc/I, because of the small difference in the moment of inertia of the open and solid sections of the beam. This assumption is tantamount to saying that secondary moment effects in throat sections due to shear are negligible. Consequently, any open-web throat section in the constant moment region became critical. Initial yield in the flange was calculated to take place at a load of 22,900 lb.

This specimen was loaded with two equal symmetrical loads 91 in. apart. Each load was $53^1/_2$ in. away from the end support (see Table 2). With such a loading system the pure bending region had seven openings, while the two end portions (shear and moment) had four each. Full bearing stiffeners were used under each load.

Loads were applied in increments to 21,000 lb at which point yield lines were first observed in the throat section flange in the region of maximum or constant moment. As higher loads were applied more yield lines appeared and spread. The load was finally increased to 24,700 lb. However, the specimen could not carry the load and final equilibrium was reached at 23,500 lb which is to be considered the maximum load. Collapse of the beam was due to excessive yielding and flange buckling. Figure 26 is a load-deflection curve for this specimen. Figure 27 shows portions of the specimen at the end of the test prior to removal from the testing machine. Excessive yielding and flange local buckling can be seen in the area of the tee section at the compression side over the second opening in the pure bending region.

Summary of Results and Conclusions

Test results are summarized in Table 3. Here the computed and observed initial yield moments are given, together with the maximum loads carried by each specimen. These loads are converted to maximum moment in the next column. The last column in this table is of interest and shows the reserve strength of such beams beyond initial yield to be about 12–15%.

No direct comparisons can be made between specimens because the moment areas were varied. For instance Specimen B was tested to failure with the loads only $40^3/_8$ in. away from the supports, while the same distance was $59^1/_2$ in. for Specimen C and $53^1/_2$ in. for Specimen A. In actuality Specimen B was penalized because the loading (for Test 3) emphasized shear instead of moment.

Specimen A failed due to bending. On the other hand, Specimens B and C failed from the combined effects of bending and shear. Consequently, relative strengths obtained from tests to failure are not quite representative, and in a way misleading. As it is indicated in Table 3, Specimen B in Test 1 at initial yield developed the highest moment carried of any specimen. This specimen was tested to failure with loads close to the end reactions.

Figure 28 presents the observed moment-deflection curves for Test 1 of Specimen B, Test 3 of Specimen C and the test to failure of Specimen A. A comparison betweeen the calculated moment-deflection curve of the unexpanded beam 10B11.5 and those of the expanded Specimens A, B and C shows that all specimens carried larger loads than the unexpanded shape. It is of interest to note the good correlation between calculated and measured deflection for the expanded beams.

Figure 29 shows moment-deflection curves of the three specimens when tests were carried to failure. The effects of high shear on this type of beam is illustrated by the performance of Specimen B. Both Figs. 28 and 29 show that depth and stiffness are related. Specimen C, which is the deepest, is stiffer than both A and B. Based on

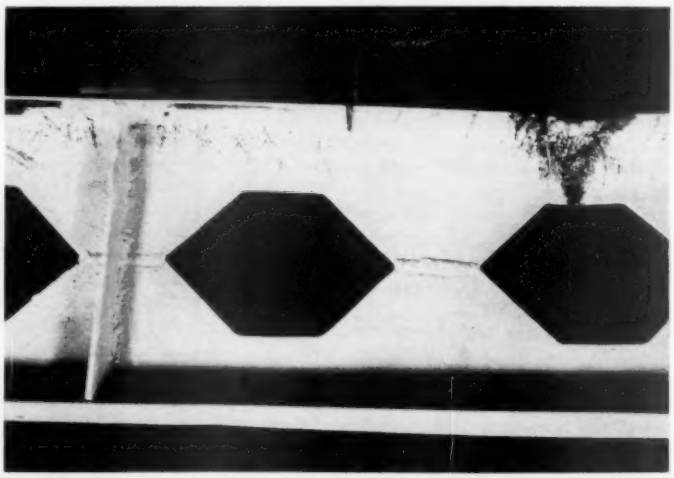


Fig. 27 Specimen A after failure. View shows excessive yielding and flange local buckling

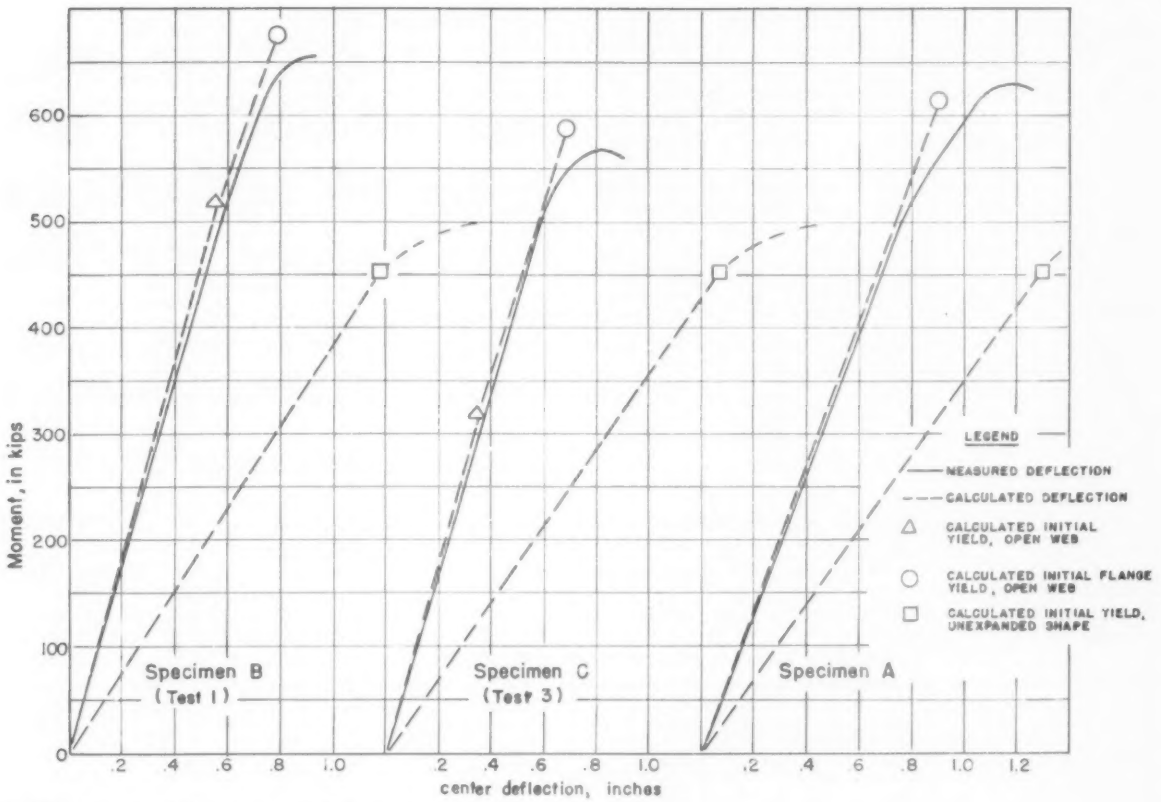


Fig. 28 Moment-deflection curves for specimens A, B and C

the above, the following conclusions may be drawn:

(*a*) All three specimens carried more moment than that calculated for the unexpanded 10B11.5 joist. (This increase for Specimen B in Test 1 is approximately 35%.)

(*b*) The observed stiffness of the expanded beams was greater than the calculated stiffness of the unexpanded beam by as much as 2 to 2.5 times.

(*c*) With better lateral support these specimens most probably would have carried higher loads.

(*d*) Specimen B has been penalized as far as loading is concerned since it was tested to failure with the applied loads very close to the end reactions.

(*e*) Open-web beams may prove to be economically feasible and competitive if the increase in carrying capacity and stiffness offsets the additional expense for fabrication.

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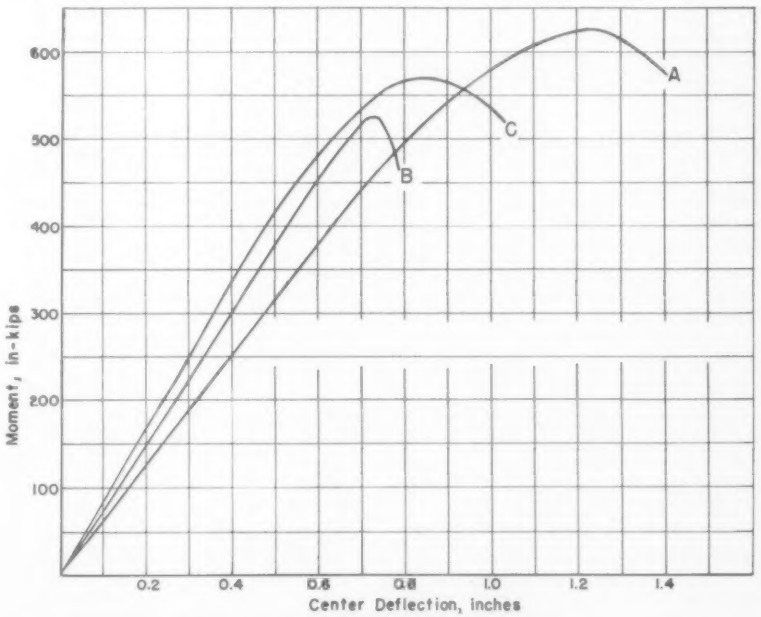

Fig. 29 Moment-deflection curves for specimens A, B and C

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Table 3—Summary of Test Results

Specimen	Initial yield moment, in-k: Computed	Initial yield moment, in-k: Observed: Deflection*	Initial yield moment, in-k: Observed: Yield line†	Observed maximum: Load, kips	Observed maximum: Moment, in-kips	Maximum observed moment/computed initial yield	Maximum observed moment/yield line moment
A	612	482	562	23.50	630	1.03	1.12
B	518‡	487	654‡	26.15§	527§	1.02	..
C	328	268	497	19.15	570	1.74	1.15

* Moment at which straight-line proportionality between load and deflection ceases.
† Moment at which yield lines are first observed.
‡ Moment arm equal to 74 in.
§ Moment arm equal to $40^3/_8$ in. (see Table 2).

APPENDIX A

SECTION PROPERTIES OF THE SPECIMENS

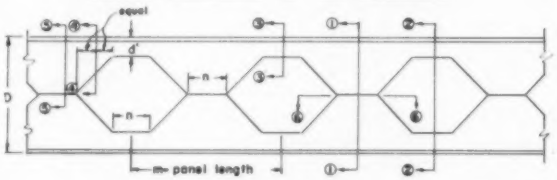


TABLE OF DIMENSIONS IN INCHES

Table Of Dimensions Based On Expanding a 10 B 11.5					
Specimen	n	d'	D	m	m/D
A	$3\,3/8$	$3\,3/8$	13.00	13.00	1.00
B	$3\,1/2$	$2\,1/2$	14.75	16.75	1.14
C	$3\,1/2$	$1\,3/4$	16.25	19.75	1.22

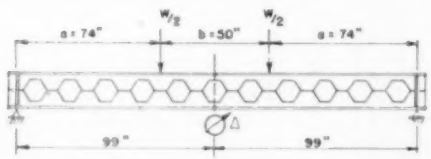
MOMENT OF INERTIA OF EXPANDED BEAMS

Moments Of Inertia – in^4						
	Section					
Specimen	①-①	②-②	③-③	④-④	⑤-⑤	⑥-⑥
A	95.90	92.24	1.55	1.61	8.72	0.58
B	129.48	115.58	0.61	1.61	12.28	0.64
C	163.35	132.26	0.22	1.61	16.95	0.64

APPENDIX B

Sample Calculations* for Center Deflections

Specimen B, Test 1 and 2



Step 1. Calculate the center deflection using moment of inertia of solid section

$$\Delta = \frac{Wa}{48EI}(3l^2 - 4a^2) = \frac{W \times 74}{48 \times 129.5 \times E}(3 \times 198^2 - 4 \times 74^2)$$

$$= 1140\,\frac{W}{E}$$

Step 2. Increase the calculated deflection of step 1. by a ratio $\gamma = \frac{I+In}{2I}$

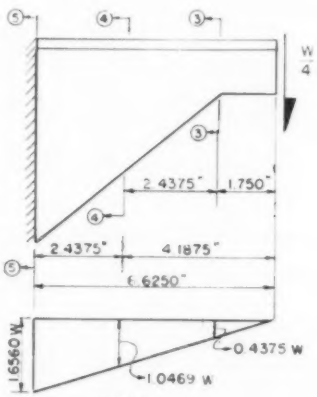
At the center line of specimen B, $\gamma = \frac{(2.105+2.216)w}{2 \times 2.105w}$

$\gamma = 1.026$

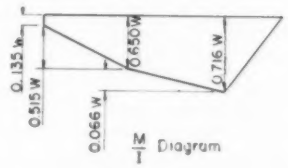
Therefore $\Delta_2 = 1.026 \times \Delta_1 = 1168\,\frac{W}{E}$.

Step 3. Calculate distortion deflection 4p as follows:

I ③-③ = 0.611 in^4
I ④-④ = 1.610 in^4
I ⑤-⑤ = 12.281 in^4



M Diagram



$\frac{M}{I}$ Diagram

$$\frac{E}{W}(\Delta p) = \frac{0.716}{2} \times 1.750 \times \frac{2}{3} \times 1.750 = 0.73$$
$$+ \frac{0.066}{2} \times 2.438 \times 2.563 = 0.21$$
$$+ 0.650 \times 2.438 \times 2.969 = 4.70$$
$$+ \frac{0.515}{2} \times 2.438 \times 5.001 = 3.14$$
$$+ 0.135 \times 2.438 \times 5.406 = 1.78$$
$$10.56$$

$$\Delta p = 21.12\,\frac{W}{E}$$

$$\Delta_3 = n\Delta p = 21.12\,\frac{W}{E} \times 4 = 84.48\,\frac{W}{E}$$

Step 4. Total Deflection then is:

$$\Delta_{\mathbb{C}} = \Delta_2 + \Delta_3 = 1252.48\,\frac{W}{E}$$

For a W = 10 kips,

and E = 30×10^3 kips per square inch

$$\Delta_{\mathbb{C}} = 1252.48 = \frac{10}{30 \times 10^3} = 0.4175 \text{ inches (See figure 18.)}$$

* More detailed examples in Reference (10) which is a thesis prepared by the senior author

THE PLASTIC DUCTILITY OF AUSTENITIC PIPING CONTAINING WELDED JOINTS AT 1200° F

Investigation includes methods of attack for solving the problem of the choice of material for main steam piping

BY R. W. EMERSON AND R. W. JACKSON

Introduction

Less than ten years ago, the first central station operating at a steam temperature of 1050° F was placed in service. The main steam piping in this unit, and others which followed in quick succession was specified as 2¼% chromium, 1% molybdenum or 3% chromium, 1% molybdenum steel, although the turbine piping used on many of these units was specified as AISI Type 347 (18% chromium, 10% nickel, columbium steel) material.

After relatively short periods of operation (6–18 months) of the 347 piping, some difficulties were encountered which subsequently led to routine surface inspection of many of the 347 lines, particularly at or directly adjacent to welds. The discovery of cracks at this location at many welded joints was of considerable concern to those directly involved, with the result that subsequent turbines for 1050° F operation were designed with 1% chromium, 1% molybdenum, 0.2% vanadium or 2¼% chromium, 1% molybdenum steel piping between the turbine stop valve and steam chest.[1]

With new and recurrent difficulties of cracking of 347 steam pipes which were originally installed in the 1050° F units, together with the fact that more recent installations have been built for 1100–1150° F and design and construction for steam conditions of 1200° F now under way (which essentially precludes the use of known ferritic steels now available), it is recognized that the problem of the choice of material for main steam piping must be solved in one of two manners (preferably both) as follows:

1. Research as to the basic cause of cracking which has been found in 347 piping quite consistently adjacent to welds, and means of overcoming this difficulty in new installations. To this end, numerous technical publications have already appeared in the literature.[1, 4, 6–10]

2. Studies on the fabrication, weldability and simulated high temperature service testing of other high temperature austenitic steels and superalloys with potential uses for piping above 1050° F. Although numerous private research projects are presently under way on austenitic steels other than Type 347 (on which the service experience has been not too favorable), very little high-temperature service experience is presently available on such materials and hence only a meager amount of information is to be found in the technical literature.

It has been the authors' belief for some time, that the apparently premature failures in Type 347 material, although originally somewhat mysterious, may be classified simply as stress to rupture failures. It was shown as early as 1946[2] that the rupture strength of Type 347 material was increased materially as the grain size was increased, but, likewise, the rupture ductility was significantly lowered, in fact to values as low as 7 to 5% at 1100 and 1200° F, respectively in specimens having an ASTM grain size of nos. 4 to 6. The rupture specimens tested were furthermore stressed uniaxially, whereas main-steam central station piping is subject to multiaxial stresses during service. In view of the above, and also due to the fact that it is difficult and highly improbable that pierced and hot-rolled seamless Type 347 pipe can be produced with a grain size equal to, or finer than, ASTM nos. 4 to 6 in pipe sizes used for main steam piping, and practically impossible to produce such a grain size in solid forged and bored pipe in this analysis, it does not seem too unreasonable that service failures have occurred in Type 347 piping with little or no apparent hot rupture ductility.

It cannot be overemphasized that an equally important consideration to high-temperature strength is high-temperature ductility to fracture, i.e., capacity to withstand plastic strain without fracture, a property which Type 347 appears to be lacking, particularly when in the coarse grained condition, and which is believed to be one of the principal causes for many of the failures in 347 piping adjacent to welded joints.

Scope

With respect to the introduction, this investigation was generally aimed at both methods of attack of the problem of the choice of materials for main steam piping.

Believing that rupture ductility is closely associated with grain size, 2½-in. diam AISI types 347 and 316 bar stock were heat treated to produce a wide variation in grain size, from which heavy-wall tubular specimens were subsequently machined. The specimens were U-beveled, welded using two different welding procedures, and the individually welded joints tested for plastic ductility, using a relaxation type test in the temperature range of 1440 to 1200° F. The testing was performed by inducing thermal strain in the test section by internally heating the tubular specimen to a maximum temperature of 1440° F. The tubular specimen which was free to expand during the heating cycle was subsequently rigidly fixed at each end, and the temperature at the center of the test section, at which the welded joint was located, then lowered to 1200° F, thus inducing mechanical (elastic plus plastic) strain into the test section while being held at 1200° F. The specimen was held at temperature from 13 to 90 hr, the longer periods representing week-end testing. At the end of the test period, the temperature of the tubular specimen was increased to a

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temperature approaching 1440° F, transforming the mechanical strain to thermal strain, the load released and the specimen cooled to room temperature. Elongation measurements were taken and the testing cycle was repeated until failure occurred, or a total plastic elongation of 0.20 in. in 20.0 in. was obtained.

The purpose of the investigation was first to determine if low-ductility failure could be induced in Type 347 heavy-wall welded tubular joints under simulated service testing similar to those which have been found to occur in actual high-temperature service. The second objective was the effect of base metal grain size on the fracture ductility as measured by specimen elongation. The third objective was to establish, if possible, a metallurgical treatment which would improve the hot plastic ductility across the welded joint such that it would be more receptive toward the acceptance of additional plastic strain (resulting from the high stresses imposed upon it) without failure. As a fourth objective it was desired to determine and compare the behavior of AISI Type 316 with that of Type 347 under similar testing conditions.

Chemistry of Materials and Its Potential Effect on Microstructure

The base metal used in the tests was taken either from the AISI Type 347 or Type 316 bar stock, the analyses of which are listed in Table 1. Also shown in Table 1 are weld metal analyses made on several of the completed test specimens. Since all the electrodes used, except the 16Cr – 8Ni – 2Mo type, were commercial lime coated E347-15 or E316-15, and furnished from a single source, it was believed unnecessary to analyze the weld deposit from each test. Deposits from test welds made with the 16-8-2 electrodes were not analyzed.

The application of the Schaeffler diagram to the analyses shown in Table 1 reveals that the as-welded 347 deposits would contain 3 to 6% ferrite, the 316 deposits would be fully austenitic and the 16-8-2 deposits would lie on the austenite—austenite plus ferrite interface. The base metals if fused using the inert-gas tungsten-arc process would possibly develop a fraction of 1% of ferrite.

Actual ferrite measurements made on as-deposited 347 weld metal were found to vary from 3.8 to 7.2% with an average value of 5.6%, whereas the 316 deposits were found to be fully austenitic. Both base metals were found to be fully austenitic except in the specimens which were heated to 2375° F in which case the 316 was found to indicate 1/4 to 1/2% of ferrite, whereas the 347 varied from 1.5 to 3.5% ferrite.

All 347 deposits after high temperature testing were found to contain an average of 1.0 to 1.5% ferrite with the loss (from

Table 1—Chemical Analyses of Materials

Material analyzed	*C*	*Mn*	*Si*	*Cr*	*Ni*	*Mo*	*Cb*
	%						
Type 347 base metal	0.060	1.80	0.73	18.61	11.07	..	0.65
Type 316 base metal	0.080	1.64	0.15	17.64	10.68	2.30	...
Type 347 weld deposit (from Test 8)	0.059	1.75	0.43	20.41	12.08	..	0.78
Type 347 weld deposit (from Test 13)	0.073	2.00	0.44	20.33	10.72	..	0.73
Type 316 weld deposit (from Test 9)	0.073	1.59	0.53	19.54	14.42	2.55	..
Type 316 weld deposit (from Test 11)	0.064	1.72	0.33	19.71	15.70	2.75	..
16-8-2 weld deposit*	0.086	1.65	0.45	15.87	8.37	1.42	...

* Not analyzed—taken from Ref. 9.

Table 2—Base Metals, Electrodes and Heat Treatments Used in Welded Pipe Joints Subjected to Elevated-Temperature Testing

Test no.	*Base metal*	*Heat treatment*	*Welding electrodes*	*Postweld heat treatment*
8 and 13	347	As-received	347	As-welded
	347	As-received		
25	347	1900° F WQ	347	As-welded
	347	1900° F WQ		
18 and 26	347	2100° F WQ	347	As-welded
	347	2100° F WQ		
	347	2300° F WQ	347	As-welded
	347	2300° F WQ		
21 and 22	347	2375° F WQ	347	As-welded
	347	2375° F WQ		
10	347	As-received	347	1900° F—2 hr—air cool
	347	As-received		
	347	1900° F WQ	347	1900° F—2 hr—air cool
	347	1900° F WQ		
27	347	2100° F WQ	347	1900° F—2 hr—air cool
	347	2100° F WQ		
	347	2300° F WQ	347	1900° F—2 hr—air cool
	347	2300° F WQ		
	347	2375° F WQ	347	1900° F—2 hr—air cool
	347	2375° F WQ		
9	316	As-received	316	As-welded
	316	As-received		
12	316	1900° F WQ	316	As-welded
	316	1900° F WQ		
14	316	2100° F WQ	316	As-welded
	316	2100° F WQ		
16	316	2300° F WQ	316	As-welded
	316	2300° F WQ		
19	316	2375° F WQ	316	As-welded
	316	2375° F WQ		
11	316	As-received	316	1900° F—2 hr—air cool
	316	As-received		
	316	1900° F WQ	316	1900° F—2 hr—air cool
	316	1900° F WQ		
15	316	2100° F WQ	316	1900° F—2 hr—air cool
	316	2100° F WQ		
17	316	2300° F WQ	316	1900° F—2 hr—air cool
	316	2300° F WQ		
20	316	2375° F WQ	316	1900° F—2 hr—air cool
	316	2375° F WQ		
1	347	As-received	347	As-welded
	347	2100° F WQ		
2 and 3	347	As-received	347	As-welded
	347	2300° F WQ		
4	347	As-received	347	As-welded
	316	As-received		
5	347	As-received	347	As-welded
	316	2300° F WQ		
6	347	2300° F WQ	347	As-welded
	316	As-received		
7	316	As-received	316	As-welded
	316	2100° F WQ		
24 and 28	347	1900° F WQ	16-8-2	As-welded
	347	1900° F WQ		
23	316	1900° F WQ	16-8-2	As-welded
	316	1900° F WQ		

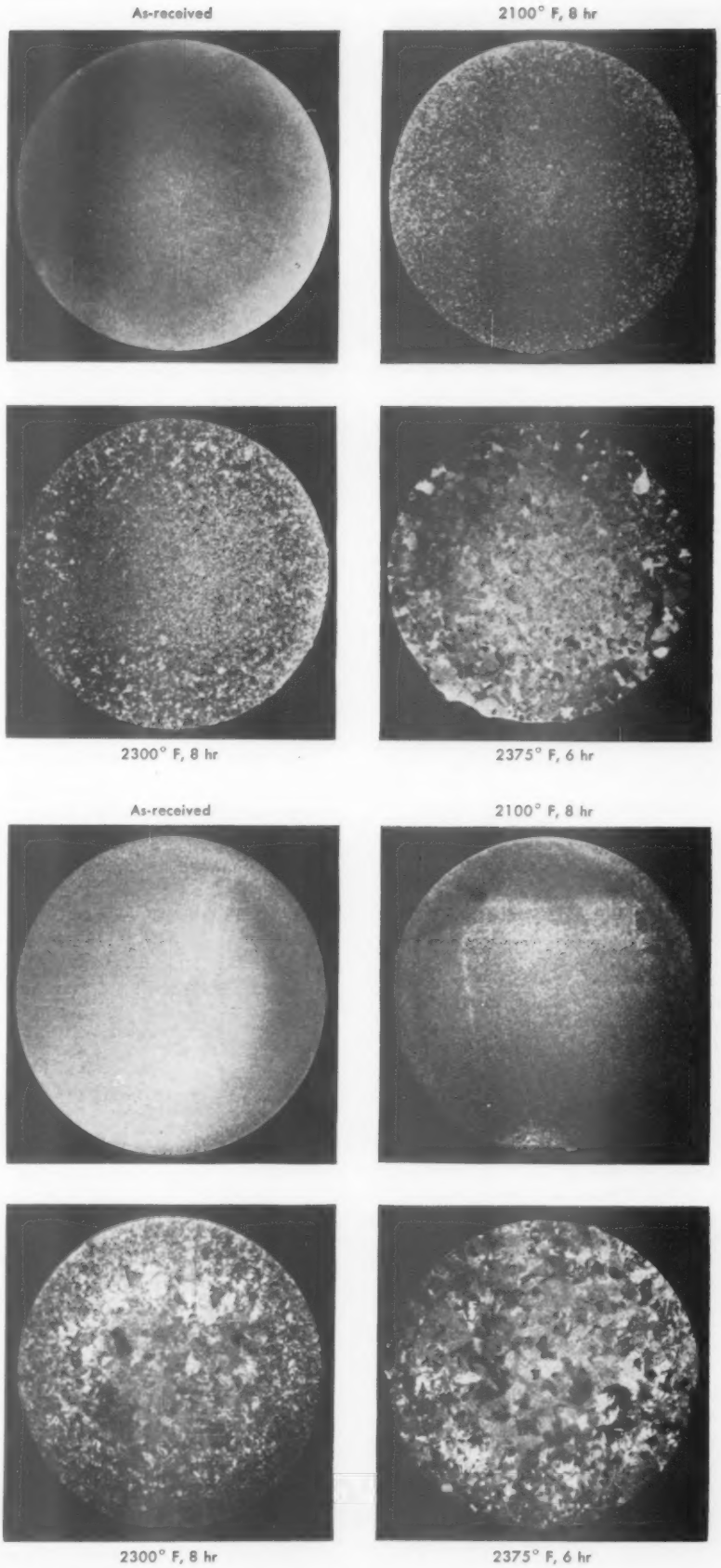


Fig. 1 Grain coarsening characteristics of AISI Type 347 bar stock from which tubular test specimens were machined. Etchant, hot 50% HCl. X 1. (Reduced by $^1/_2$ upon reproduction)

the as-deposited condition) due to the formation of sigma phase except for those specimens given a 1900° F post-weld heat treatment, in which case the loss was due to the actual solution of ferrite in the austenite during the heat treatment. The 316 deposits after testing were fully austenitic and the 16-8-2 deposits were found to contain 3.2% ferrite.

The Effect of Heat Treatment on Grain Size and Room-Temperature Mechanical Properties

A portion of both the Type 347 and Type 316 bar stock was originally heated to 1900, 2100 and 2300° F for 8 hr and water quenched. The grain size and room temperature mechanical properties were subsequently obtained on this material, as well as on that in the as-received condition.

Prior to the conclusion of this investigation a few elevated-temperature tests were made from bar stock which was coarsened by heating to 2375° F for 6 hr. Room-temperature mechanical tests were not, however, made on this material.

The grain coarsening characteristics of both the types 347 and 316 bar stock material are shown in Figs. 1 and 2 at a magnification of × 1. A metallographic examination of this material in the various conditions of heat treatment revealed the following ASTM grain size:

	Type 347	*Type 316*
As-Received	6–7	3–6
1900° F	4–7	3–6
2100° F	1–4	0–1
2300° F	0–4	>00–0
2375° F	>00–0	>00

It is quite obvious from Figs. 1 and 2 that a reasonable variation in grain size exists within a given specimen, particularly in the material heat treated at the higher temperatures. That even greater variations in grain size actually may exist in production material is evidenced by Fig. 3 which shows a partial ring section removed from a $10^3/_4$-in. OD x $1^1/_8$-in. wall forged and bored Type 347 pipe used for nuclear power service.

Photomicrographs of the 347 and 316

Fig. 2 Grain coarsening characteristics of AISI Type 316 bar stock from which tubular test specimens were machined. Etchant, hot 50% HCl. X 1. (Reduced by $^1/_2$ upon reproduction)

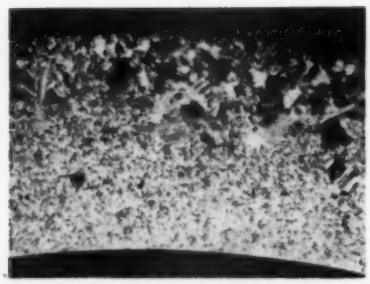


Fig. 3 Partial ring section removed from $10^{3}/_{4}$ in. OD x $1^{1}/_{8}$-in. wall forged and bored AISI Type 347 material exhibiting a large variation in grain size. Etchant, hot 50% HCl. X 1

material are shown in Fig. 4 in the as-received and 2300° F heat-treated condition. It may be noted that the 316 has a somewhat coarser grain size than that of the 347, as well as indicating some degree of cold work. The 316 also has a somewhat coarser grain structure than the 347 after the 2300° F heat treatment.

The effect of heat treatment on the room-temperature mechanical properties of both types 347 and 316 is shown in Fig. 5. In general, an increase in the heat treating or solution treating temperature results in a lowering of the tensile strength, yield strength and Brinell hardness, with an increase in the elongation and reduction of area. Figure 5 reveals, however, two exceptions to the above generalities, namely a yield strength in Type 316 which is higher in the 2100° F heat-treated condition than in the 1900° F heat-treated condition, and a substantial loss in both elongation and reduction of area of the 347 when heat treated at 2300° F as contrasted with the 2100° F heat treatment. Both exceptions will be further discussed. It is believed that the latter exception may have some bearing on the relatively premature failures subsequently obtained in high temperature testing which was not evident in the 347 material heat treated at lower temperatures. The fractured room temperature Type 347 tensile specimens are shown in Fig. 6. It is to be noted that the surface mottling of the specimens becomes increasingly apparent as the solution treating temperature is increased. The surface appearance of the 316 specimens was essentially identical to that shown in Fig. 6, with the exception that the coarse grained 347 specimen gave indication of numerous surface microfissures which was not evidenced in the corresponding Type 316 specimen.

Fabrication Procedure for High-Temperature Test Specimens

The bar stock in the various conditions of heat treatment was subsequently cut into 4-in. lengths and bored to an ID of $^{5}/_{8}$ in. The ends were U-beveled,

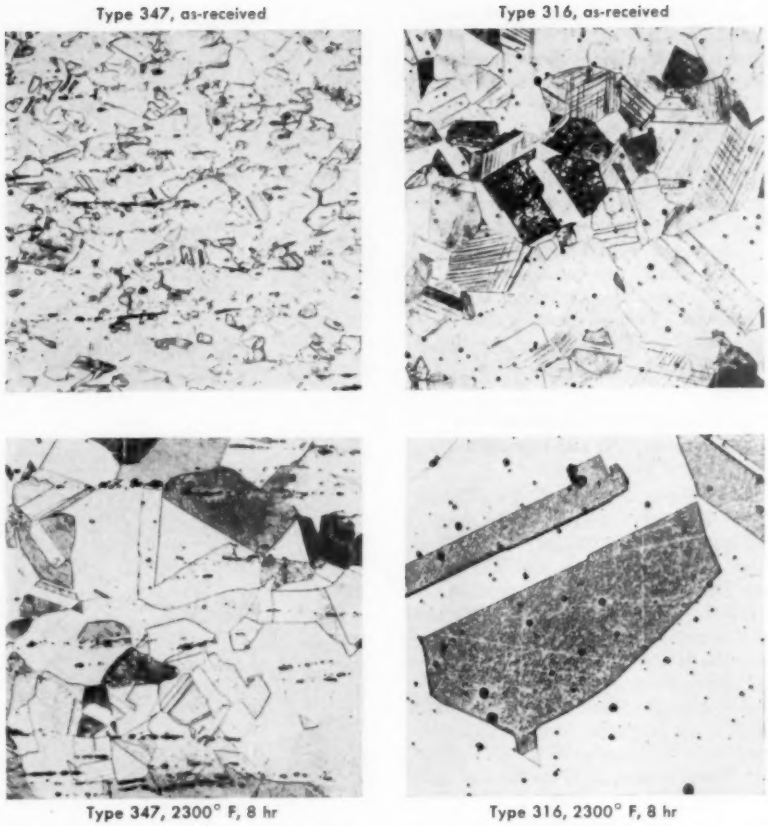


Fig. 4 Grain size and structure of bar stock used for tubular specimens. Etchant, electrolytic 10% oxalic acid. X 100

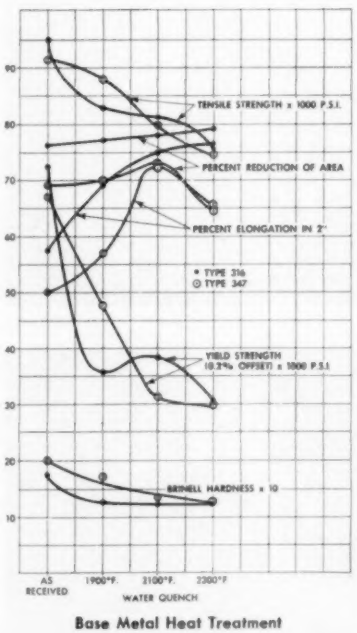


Fig. 5 The Effect of heat treatment on the room-temperature mechanical Properties of AISI Types 316 and 347 stainless steel

Fig. 6 Effect of grain size on the surface mottling of room-temperature tensile specimens machined from Type 347 bar stock. X $^{1}/_{2}$

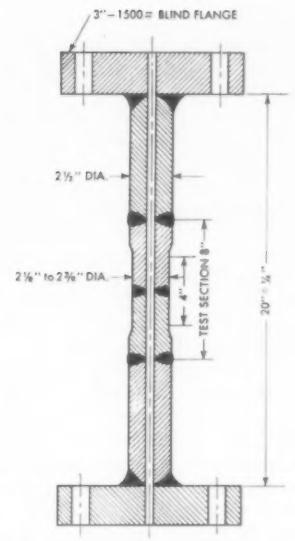


Fig. 7 Test weld assembly prior to installation in testing frame

Fig. 8 Test frame for elevated-temperature testing of welded tubular specimens with flanged weld test assembly in place. X 0.07

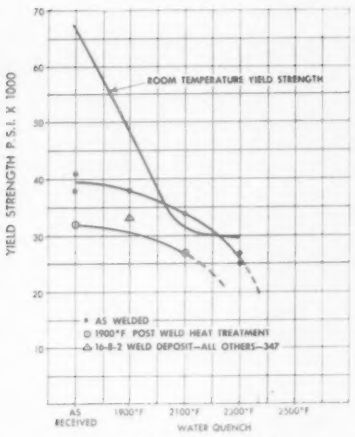


Fig. 9 The effect of base-metal heat treatment and postweld heat treatment on the yield strength of AISI Type 347 welded pipe joints at 1200° F

welded with an inert-gas tungsten-arc root pass and completed using the manual metal-arc process. The welds were made without preheat and a maximum interpass temperature of 250° F. The completed weld specimen which was approximately 8 in. in length was subsequently welded to two pipe stubs of similar cross section, the opposite ends of which had been previously welded to 3 in., 1500 psi steel blind flanges. These welds were made substantially identical to that of the center test weld. Some of the welds were tested at 1200° F in the as-welded condition, while others were tested at this temperature in the as-welded plus 1900° F air cooled condition. Tests which were given the 1900° F postweld heat treatment were, of course, given this treatment upon completion of the test weld and prior to welding the test specimen to the flanged stubs. After completing the flanged test assembly, the center test weld was turned smooth to approximately $2^1/_4$ in. OD for a distance of 2 in. each side of the weld, the ID rebored to $^5/_8$- and a $^3/_{32}$-in. hole drilled in the test weld for the insertion of a control thermocouple. A sketch of the completed test weld assembly is shown in Fig. 7.

Procedure Used in Making High-Temperature Tests

The test frame into which the flanged ends of the completed weld test assembly were bolted, consisted of two end plates $13^1/_2$ x 21 x 6 in. thick, which were rigidly supported by six 2-in., B-7 chrome-moly bars used for high-temperature bolting, having a minimum yield strength of 105,000 psi. Each end of the bars was threaded for a length of 15 in. such that the end plates were adjustable in length by changing the location of nuts on either side of the two end plates as shown in Fig. 8. With the 3-in. flanges bolted tight to the two end plates, the bottom end plate rigidly fixed with respect to the six through-bolts, and all nuts on the through-bolts free at the top, to provide free movement of the top end plate, a $^1/_2$ in. diam by 30-in. long, 220 v, 1100-w tubular heater was inserted through the top end plate for the purpose of internally heating the flanged test assembly. With a control thermocouple located at the center weld under test and $2^1/_2$ in. of high temperature insulation surrounding the flanged test assembly, the test specimens were heated to 1440° F with the top end plate free floating. Upon reaching this temperature, however, all nuts on the under side of the top end plate were driven tight with an open end wrench and a 6-lb hammer, and the test temperature lowered to 1200° F. This temperature was held overnight or over week ends. Each morning the test temperature was again raised to provide sufficient thermal strain to relieve the compression on the nuts, the nuts loosened and the temperature then lowered to room temperature. The above cycle was then repeated until

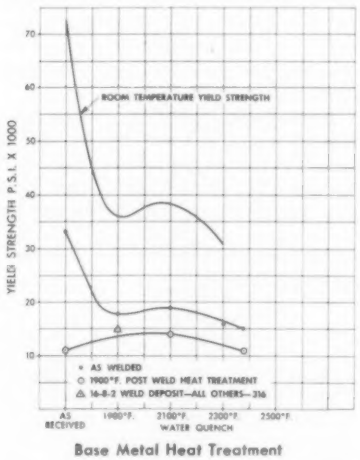


Fig. 10 The effect of base-metal heat treatment and postweld heat treatment on the yield strength of AISI Type 316 welded pipe joints at 1200° F

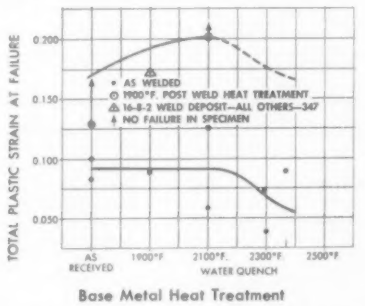


Fig. 11 The effect of base-metal heat treatment and postweld heat treatment on the total plastic strain at failure of AISI Type 347 welded pipe joints at 1200° F

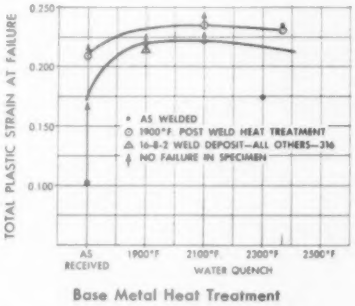


Fig. 12 The effect of base-metal heat treatment and postweld heat treatment on the total plastic strain at failure of AISI Type 316 welded pipe joints at 1200° F

Table 3—Plastic Ductility (Total Plastic Strain), Average Elastic Strain and Duration of Welded Tubular Specimens Under Test at 1200° F

Test no.	Total plastic strain, in.*	Avg elastic strain, in.*	Duration of test at 1200° F, hr/min
8	0.100	0.038	93-50
13	0.083	0.041	110-00
25	0.089	0.038	281-45
18	0.058	...	84-20
26	0.126	0.034	186-55
21	0.089	0.014	29-45
10	0.128	0.032	235-20
27	0.201	0.027	249-00
22	No test	...	....
9	0.101	0.033	182-45
12	0.221	0.018	226-00
14	0.223	0.019	167-45
16	0.173	0.016	133-40
19	0.235	0.015	216-40
11	0.209	0.011	132-40
15	0.235	0.014	152-35
17	No test	...	....
20	0.231	0.011	167-05
1	...	...	....
2	...	...	135-10
3	0.038	0.025	106-20
4	0.019	0.032	152-20
5	0.075	0.023	149-25
6	0.073	0.027	198-15
7	0.217	0.020	297-10
24	0.126	0.028	116-15
28	0.172	0.033	280-20
23	0.217	0.015	145-25

* Specimen length 20 in. ± 1/4.

a total plastic strain of 0.20 in. in approximately 20 in. was obtained, or the specimen failed.

It was the original intent of this investigation to determine if failure could be made to occur in the base metal adjacent to welds in stainless steel piping under laboratory conditions similar to that which has been found to occur in many instances in actual service. After making several tests, it was found that service failures were quite readily duplicated. Measurements were made between the inside of the top and bottom flange when in the cold condition, when the temperature at the center of the specimen was 1440° F and the top end plate was floating, and after driving the nuts on the under side of the top end plate tight and prior to lowering the temperature. In each instance, eight measurements were made between each of the flange bolts and the average used for subsequent calculations.

High-Temperature Testing

A total of 28 high-temperature tests were made comprising as-received, fine and coarse grain annealed 347 and 316 base metal welded to similar material, both in the as-welded and 1900° F postweld heat treated (air cooled) condition. Similar materials of dissimilar heat treatment were also welded and tested as were welded joints of dissimilar analysis having both similar and dissimilar heat treatments. In addition, two tests using 347 and a third using 316 material were welded using 16-8-2 electrodes.

A complete list of the combinations of materials and their heat treatment which were welded and tested is given in Table 2.

Measurements were made on all tests to determine the total plastic strain (elongation) after failure or at the completion of the test as well as determine the average elastic strain in the specimens during test. Due to the voluminous nature of the measurements, only the total plastic strain and average elastic strain, together with the total time of the test at 1200° F, is summarized in Table 3. By way of indicating how the data in Table 3 were obtained, all pertinent data for a single test No. 10 are given in Table 4.

From the values of the average elastic strain given in Table 3, Figs. 9 and 10 were derived showing the effect of base-metal heat treatment and postweld heat treatment on the elevated-temperature yield strength of welded pipe joints in 347 and 316 material. Also plotted is the room-temperature yield strength of the base metal as a function of base-metal heat treatment.

Since the length of the welded test assemblies were 20 in. ± 1/4 between the backs of the two flanges, and the modulus of elasticity is given as 20,000,000 to 22,000,000 psi for AISI 300 series austenitic material at 1400 to 1200° F by Timkin,[3] the elevated-temperature yield strengths were approximated by the following formula.

$$E = S/\epsilon \quad \text{or} \quad S = E\epsilon$$

where

E = modulus of elasticity = 20,000,000 psi.
ϵ = unit strain, in. per in. = 0.032 in./20 in. (See Table 4 for value of 0.032 in.)
S = unit stress, psi.
$S = E\epsilon = 20{,}000{,}000 \times (0.032/20) = 32{,}000$ psi.

By using a modulus value of 20,000,000 psi and a specimen length of 20 in., it may be seen that the formula simplifies to the extent of moving the decimal point in the average elastic strain values in Table 3, six places to the right to obtain the unit yield strength of the test specimens at 1200° F.

Since the test specimen yields plastically upon cooling from 1440 to 1200° F until the load is reduced to that representing the high-temperature yield strength value of the specimen and the

Table 4—Actual Measurements and Derived Data from High-Temperature Test 10

Test 10	Thermal strain (70–1200° F) free*	Thermal strain (70–1440° F) free	Total thermal plus mechanical strain (70–1440° F) (end plates tightened)	Total elastic plus plastic strain at 1200° F, col. 4 minus col. 2	Total plastic strain (no load cold to no load cold)	Total plastic strain after completing test (sum of col. 6)	Elastic strain, col. 5 minus col. 6	Avg elastic strain	Duration of heating and loading cycles at 1200° F, hr-min	Total duration of test (sum of column 10), hr-min
a	0.172	0.208	0.220	0.048	0.018		0.030		63-50	
b	0.171	0.207	0.219	0.048	0.015		0.033		16-25	
c	0.169	0.205	0.219	0.050	0.016		0.034		17-55	
d	0.170	0.206	0.224	0.054	0.025		0.029		17-10	
e	0.171	0.207	0.224	0.053	0.021		0.032		15-10	
f	0.168	0.203	0.220	0.052	0.019		0.033		88-20	
g	0.168	0.203	0.216	0.048	0.014	0.128	0.034	0.032	16-30	235-20

* Calculated: 1200 − 70 = 1130° F; 1440 − 70 = 1370° F.
(1130/1370) × free thermal strain at 1440° F (col. 3) = 1200° F thermal strain (col. 2).

specimen then held under this load for periods of 13 to 90 hr, the test becomes one of a stress to rupture type with the stress value in each instance being that of the 1200° F yield strength.

As an indication of the excellent high-temperature strength of 347, it is to be noted from Table 4 that the elastic strain remained independent of the time duration of the cycles performed in making the test.

Although the absolute yield strength values, due to the approximations used, may not be entirely correct, it is believed that a comparison of the individual curves in the two figures is quite revealing.

From Fig. 9 it may be noted that increasing the solution heat-treating temperature, and thereby increasing the grain size, results in a lowering of the room-temperature yield strength of the base metal, as well as the 1200° F yield strength of the welded pipe specimens. It may be noted also that the elastic properties of the as-welded joints are exceedingly high; in fact, the results of the tests indicate the yield strength of the 2100° F solution treated material in the as-welded condition at 1200° F to be higher than that obtained from a 0.505-in. diam tensile specimen of the base material tested at room temperature. While these data were not confirmed by additional tests, they do fit the other data plotted on the as-welded curve. It is not beyond the realm of possibility that such a condition of high elasticity (yield strength) actually exists across as-welded Type 347, heavy-wall cylindrical joints as used in high-temperature central station piping due at least in part to multiaxial combined stresses not present in small diameter solid-bar test specimens. It is, in fact, believed to be due to the high yield strength of 347 at conventional operating temperatures of 1050–1200° F, which is substantially above the long time, and, in many instances, short-time rupture strength of the material, that it fails by stress to rupture rather than yielding or deforming plastically to accommodate the imposed stresses. Further reference to Fig. 9 indicates that a 1900° F postweld heat treatment does lower the high temperature yield strength by 7000 to 8000 psi, with values still in the range of 30,000 psi. These data are in good agreement with other recently published rupture strength data.[1] It also may be noted that the as-welded 347 material using 16-8-2 electrodes had a yield strength which was only 5000 psi lower than the test using 347 electrodes and comparable base-metal heat treatment and a yield strength equal to and greater than respective as-welded tests with base-metal heat treatments of 2100 and 2300° F using 347 electrodes.

From Fig. 10 it may be noted that the 1200° F yield strength across both as-welded and postweld heat-treated joints in Type 316 material is substantially below that of Type 347, as well as substantially below the room temperature yield strength of the 316 base material. The postweld heat treatment lowered the 1200° F yield strength values of Type 316 an amount similar to that shown for the 347, the shape of the

Table 5—Summary of Results of High-Temperature Tests Made on Welded Heavy-Wall Pipe Joints of AISI Types 347 and 316 Stainless Steel

	Failure				Heat treatment before testing							
	Location				Base-metal heat treatment					Electrode and postweld heat treatment		
Test no.	W*	HAZ†	BM‡	Material	AR§	1900	2100	2300	2375	AW‖	1900	Remarks
8	x	..	..	347	347	..	..	..	..	347	..	Fuse blew out at 1200° F allowing specimen to cool to room temperature. Centerline weld failure.
13	x	..	..	347	347	..	..	..	..	347	..	Centerline weld failure as Test 8 above, but no heating failure.
25	x	..	..	347	..	347	..	..	..	347	..	On last (eleventh) heating specimen failed upon cooling to room temperature with bolts tightened.
18	..	x	..	347	..	..	347	..	.	347	..	Specimen failed prematurely in HAZ of joint 4 in. from test weld.
26	x	..	..	347	..	..	347	..	..	347	..	Heater burned out and specimen cooled to 520° F after 40 min at 1200° F.
21	..	x	x	347	..	..	..	..	347	347	..	Cracks present in base metal after heat treatment and before welding and testing. Failed in HAZ of joint 4 in. from test weld.
10	..	..	..	..	347	..	..	..	..	..	347	No failure.
27	..	..	..	..	..	..	347	..	..	..	347	No failure.
22	..	..	..	347	..	..	..	..	347	347	..	No test—cracks present in base metal after heat treatment and before welding. No postweld heat treatment.
9	..	..	..	..	316	..	..	..	..	316	..	No external evidence of failure—sectioning revealed several weld metal microfissures believed to be present before testing.
12	..	..	..	..	..	316	..	..	..	316	..	No failure.
14	..	..	..	..	..	..	316	..	..	316	..	No failure.
16	..	x	x	316	..	..	..	316	..	316	..	Cracks at ID of 316 base metal as well as in HAZ.
19	..	x	x	316	..	..	..	..	316	316	..	Rupture cracks at HAZ as well as numerous others throughout base metal.
11	..	..	..	..	316	..	..	..	..	..	316	No failure.
15	..	..	..	..	..	..	316	..	..	..	316	No failure.
17	..	..	..	316	..	..	..	316	..	..	316	No test—cracks found in HAZ before testing.
20	..	x	..	316	..	..	..	..	316	..	316	One crack about 1/4 in. deep.
1	..	..	x	347 2100° F	347	..	347	..	..	347	..	Failure in reduced section base metal at sharp cornered fillet.
2	..	x	..	347 2300° F	347	..	..	347	..	347	..	No remarks.
3	..	x	x	347 2300° F	347	..	..	347	..	347	..	Rupture cracks starting at OD base metal at location of thermocouple tack welds.
4	..	x	..	347	347 / 316	..	..	..	..	347	..	Failure between butt weld and thermocouple tack weld.
5	..	x	x	316	347	..	..	316	..	347	..	Several rupture cracks starting at ID of 316 base metal.
6	..	x	x	347	316	..	..	347	..	347	..	Several rupture cracks starting at OD of 347 base metal at location of thermocouple tack welds.
7	..	..	..	..	316	..	316	..	..	316	..	No failure.
24	FL**	..	..	..	..	347	..	..	..	16-8-2	..	Fusion line failure.
28	FL**	..	..	..	..	347	..	..	..	16-8-2	..	Fusion line failure.
23	..	..	..	..	..	316	..	..	..	16-8-2	..	No failure.

* W, weld. † HAZ, heat-affected zone. ‡ BM, base metal. § AR, as-received. ‖ AW, as-welded. **FL, fusion line.

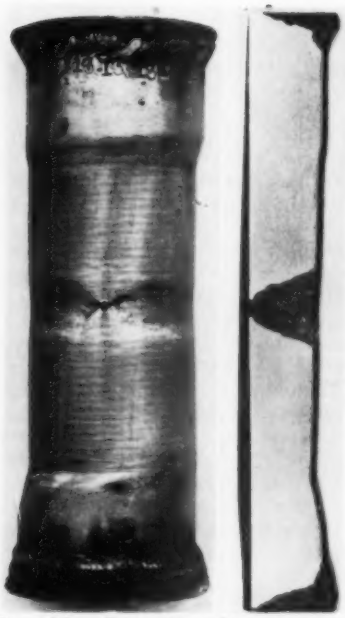


Fig. 13 Test 13 after removing flanged ends (*left*), and etched section through pipe wall (*right*). Exhibiting centerline weld failure. Etchant, hot 50% HCl. X 1. (Reduced by 1/2 upon reproduction)

curves in each case following reasonably that of the as-welded curves.

The yield strength of the 2100° F solution-treated Type 316 was found to be higher than that obtained on material solution treated at either 1900 or 2300° F. The yield strength of 316 when tested in the as-welded condition using 16-8-2 electrodes may be noted to be only 15,000 psi as against 33,000 psi when used for the welding of 347. This, as cited in the literature, is believed to be due to the fact that the performance of a composite joint consisting of weld metal, heat-affected zone and base metal is determined by that unit of the composite having properties of the lowest value; in this case the Type 316 material.[4]

The total plastic strain at failure as given for the various tests in Table 3 is plotted in Figs. 11 and 12 as a function of the base-metal heat treatment and postweld heat treatment. Referring to the 347 material in Fig. 11, it may be noted that there is considerable spread in the data for the tests made in the as-welded condition. Nevertheless it may be noted that with one exception, all tests failed with less than 0.100 in. of plastic strain in the 20-in. specimen length, and very poor ductility exhibited in specimens in which the base metal was heat treated at 2300–2375° F.

Greater plastic deformation was obtained from the as-welded test using 16-8-2 electrodes due, no doubt, to the ability of the 16-8-2 itself to plastically deform during welding and thus impose less stress on the adjacent base metal as

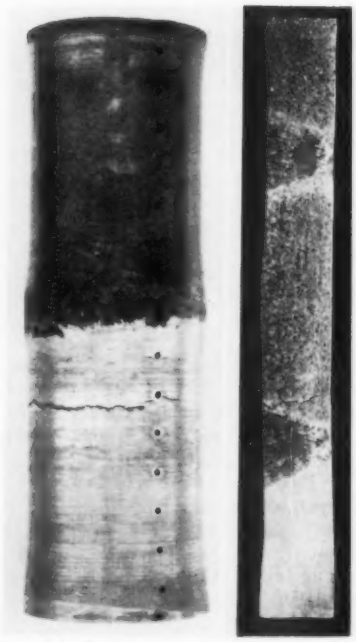


Fig. 14 Test 2 after removing flanged ends (*left*) and etched section through pipe wall (*right*) exhibiting characteristic weld heat-affected zone failure. Etchant, hot 50% HCl. X 1. (Reduced by 1/2 upon reproduction)

well as to plastically deform under test.

The two specimens (Tests 10 and 27) which were given a postweld heat treatment, however, had plastic strains of 0.128 and 0.201 in. with respective testing times of 235 hr 20 min, and 249 hr, and both were without failure upon concluding the tests.

Fig. 15 Etched section through pipe wall of test 6 exhibiting weld heat-affected zone failure in coarse grain 347. Etchant, hot 50% HCl. X1. (Reduced by 1/2 upon reproduction)

Fig. 16 Etched section through pipe wall of test 6 revealing weld heat-affected zone failure as well as stress-rupture cracks emanating from cracks in thermocouple tack welds. Etchant, Vilella reagent. X 2. (Reduced by 1/2 upon reproduction)

For this reason this curve is projected upward indicating that these tests would have withstood additional plastic deformation before failure. Whereas the plastic ductility of the as-welded 347 did not exceed 0.100 in., the postweld heat-treated material may be considered to be in the range of 0.200 in.

Further reference to Fig. 12 indicates, however, that the 316 material quite readily falls into the category of 0.200–0.250 in. of plastic strain before failure, both in the as-welded as well as postweld heat-treated condition with the postweld heat treatment providing a slight improvement in plastic strain over the as-welded condition. It is to be noted that six out of the nine tests plotted were concluded with no evidence of failure.

Results of Metallurgical Examination

The results of the high-temperature tests made on the welded heavy-wall pipe joints are summarized and condensed in Table 5. From this table it may be noted that from the 28 tests conducted, four of the specimens (all of Type 347) had centerline weld failures, two specimens failed along the fusion line, ten failed in the weld heat-affected zone of the base metal and one failed in the base metal due to stress concentration at the root of a fillet. Of the ten specimens which failed in the weld heat-affected zone, it may be noted that stress-rupture cracks were also observed in the base metal in six of these tests.

Of the remaining tests, two were not tested due to the discovery of cracks in the base metal and heat-affected zone

Fig. 17 Etched section through pipe wall of test 20 exhibiting weld heat-affected zone failure in coarse grain 316. Etchant, hot 50% HCl. X 1. (Reduced by $^1/_2$ upon reproduction)

Fig. 18 Etched section through pipe wall of test 5 revealing weld heat-affected zone failure. Etchant, hot 50% HCl. X $2^1/_2$. (Reduced by $^1/_2$ upon reproduction)

Fig. 19 Weld heat-affected zone rupture failure in fine grain 347 from Test 4. Etchant, hot 50% HCl. X $2^1/_2$. (Reduced by $^1/_2$ upon reproduction)

prior to high-temperature testing, and the tests on nine specimens were concluded before failure.

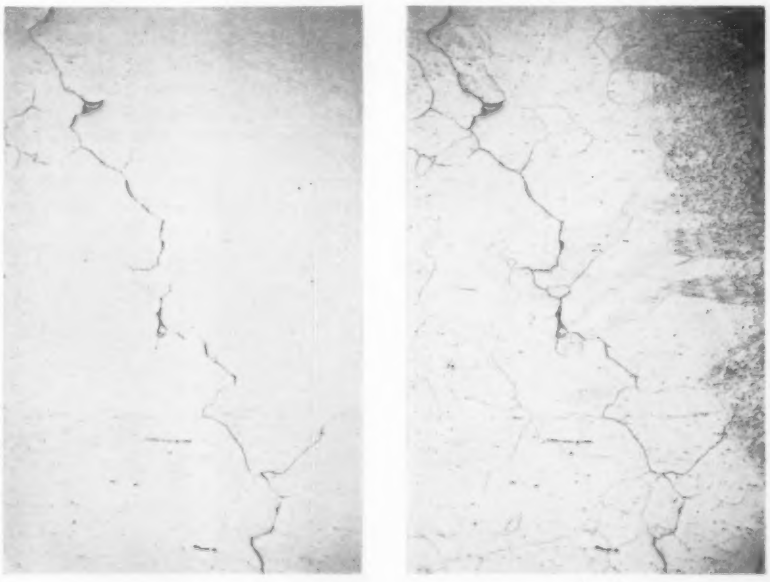
Again due to the voluminous nature of the data, the examples and discussion will be limited to the general categories outlined above.

Centerline Weld Failures

Test 13, after testing and removing the flanged ends, is shown in Fig. 13, together with a macro section through the pipe wall. The as-received 347 was found to have a relatively high room-temperature yield strength and may have been furnished in the cold finished condition. Cold working of 347 has been shown to raise the short-time stress-rupture strength of this material.[5] It is believed that this may be the cause for failure in the weld deposit rather than in the base metal, since failure usually is found to occur in the base metal adjacent to the weld in actual service. This latter type of failure was confirmed in numerous of the high temperature tests.

Weld Heat-Affected Zone Failures

The first two high-temperature weld test assemblies actually contained two test welds rather than one. Test 2 is shown with one of several sections removed from this test, after testing, in Fig. 14, indicating failure in the weld heat-affected zone of the coarse grain 347. Further confirmation of failure in the heat-affected zone of coarse grain 347 are Figs. 15 and 16 taken from two sections of Test 6. The stress-rupture crack in the coarse grain material adjacent to the weld in Fig. 15 is not too discernible as a result of the etching required to demonstrate the extreme contrast in grain size between the coarse grain 347 and relatively fine grain Type 316. The section taken adjacent to that shown in Fig. 15, however, distinctly reveals the weld heat-affected zone rupture failure as well as rupture failures occurring at the locations of numerous tack welds used for securing thermocouples to the test specimen during testing. This is presented as Fig. 16. Figures 17 and 18 are further evidence of weld heat-affected zone ruptures as taken from Tests 20 and 5, respectively, the latter of which also reveals the start of a stress-rupture crack at the ID $1^1/_8$ in. removed from the line of fusion. One additional failure is shown in Fig. 19 which occurred through relatively fine grain 347 material. Two small fissures may be noted in the weld deposit under the thermocouple tack weld on the lower side of the test weld. Photomicrographs revealing the intergranular nature of the stress-rupture cracks in the weld heat-affected zone of coarse grain 347 and 316, and fine grain 347 material are presented, respectively, in Figs. 20 and 21, Figs. 20*a* and 20*b* representing the



a b

Fig. 20 Weld heat-affected zone intergranular stress-rupture cracks. Unetched (a) and etched (b) in coarse grain 347 from Test 3. Etchant, Vilella reagent. X 70. (Reduced by 40% upon reproduction)

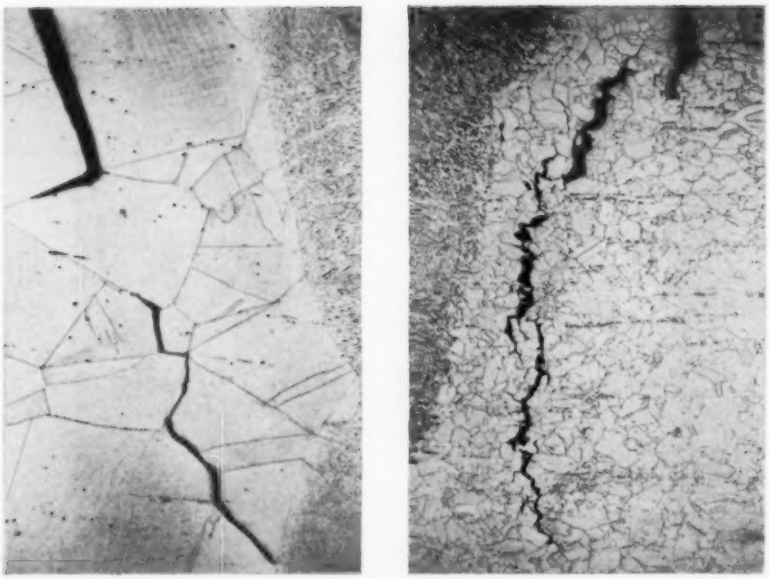


Fig. 21 Weld heat-affected zone intergranular stress-rupture cracks in coarse grain 316 (Test 5, *left*) and fine grain 347 (Test 4, *right*). Etchant, Vilella reagent. X 70. (Reduced by 40% upon reproduction)

same area in the unetched and etched condition. As was previously mentioned, several of the specimens were found to contain isolated stress-rupture cracks in the base metal and not in the immediate vicinity of the heat-affected zone.

A photomicrograph representing this condition as found in coarse grained 347 (Test 3) is shown in Fig. 22.

Stress Concentration Failure

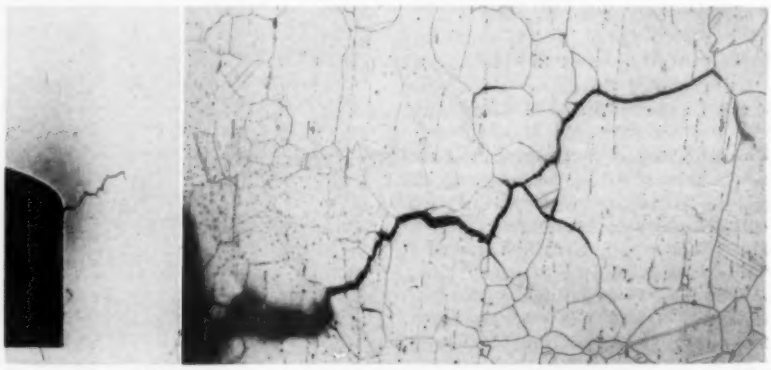
Weld test assembly No. 1, as was the case with No. 2, was fabricated with three short sections of pipe and two test welds rather than the single test weld used on the balance of all specimens. In an endeavor to force failure to occur in the first test specimen, each of the two welds, including approximately $^1/_2$ in. of base metal on each side of the weld, was machined to $2^1/_8$ in. diam. Failures have been reported to occur in the fillets of coarse grain 347 base metal and in all weld-metal specimens during stress-rupture testing due to stress concentration at this location.[2, 4] It was not known, therefore, whether failure would occur in the weld heat-affected zone of the base metal, or at the base of the relatively sharp fillet. Reference to Fig. 23 reveals the location of failure to be in the base-metal fillet about $^1/_2$ in. above the lower weld. Upon close observation, stress-rupture cracks may be seen in each of the two fillets above the failure.

Fig. 22 Stress-rupture cracks in coarse grain 347 (Test 3) not located in weld heat-affected zone. Etchant, Vilella's reagent. X 70. (Reduced by 40% upon reproduction)

Fig. 23 Test 1 after removing flanged ends (*left*), and etched section through pipewall (*right*), exhibiting stress concentration failure in coarse-grain 347 base metal. Etchant, hot 50% HCl. X 1. (Reduced by $^1/_2$ upon reproduction)

Figure 24 is presented in evidence of the rupture cracks in the base metal fillets in the proximity of the upper weld in Fig. 23. This figure clearly illustrates the intergranular nature of the rupture cracks. Of more than passing interest is the fact that the cracks in the two upper fillets and the failure all occurred in coarse-grain 347 with no evidence of cracking found in the fine-grain 347 at the lower fillet.

Material when subjected to high localized stresses or stress concentrations of a sufficient magnitude will yield or strain under the load in an effort to relieve the stress. Both types 347 and 316, as well as all of the common AISI 300 series stainless materials have an excellent capacity for withstanding



X 9 X 70

Fig. 24 Stress concentration failure in upper fillet of Test 1 shown in Fig. 23. Etchant, Vilella's reagent. (Reduced by 40% upon reproduction)

plastic strain at room temperature, as evidenced from the values of elongation and reduction of area plotted in Fig. 5. Unfortunately, however, these materials are strain sensitive at temperatures of 1000° F and above, and have only a limited ability to withstand plastic strain at these temperatures, the sensitivity to strain increasing as a function of increasing grain size. This problem is particularly pertinent in the case of Type 347 material.

Fusion Line Failure

Tests 24 and 28 in which 347 was welded with the 16-8-2 electrodes and tested in the as-welded condition were the only ones which failed at the ID and progressed toward the OD while following the line of fusion. The failures were not anticipated and the path of failure totally unexpected. The 16-8-2 weld deposit was found to have adequate capacity for plastic strain at the test temperature as indicated by the elongation of the thermocouple hole and reduced section through the weld of Test 24 shown in Fig. 25. This is further substantiated by the "necking" of the weld face of specimens removed from both tests 24 and 28, and presented in Fig. 26. This type of failure may be explained by the fact that the face of the weld is approximately five times the width across the root of the weld, thus the total potential elongation is less and the increase in loading more rapid at the root of the weld than at the face, with failure occurring in the 347 directly adjacent to the 16-8-2 deposit. The fact that failure progressed through the 347 base metal at the line of fusion rather than following through the weld deposit, as in the case of Tests 8 and 13, is believed to substantiate the fact that 16-8-2 is much less susceptible to interdendritic stress-rupture failure than is 347 weld metal.

As these tests failed from the ID toward the OD, which is contrary to the direction of failure of all other tests which failed in the base metal and contrary to experience with austenitic service failures, further laboratory testing will be required. Fracture initiation from the ID is potentially more serious than that from the OD since there would be a lesser chance of detecting the former by routine OD surface inspection.

Specimens Containing Cracks Prior to Testing

Tests 17, 21 and 22 were found to contain cracks before high-temperature testing. Since the 316 base metal in Test 17 was coarsened at 2300° F and the 347 base metal in Tests 21 and 22 was coarsened at 2375° F prior to welding, the grain boundary liquation theory may be cited as being applicable to these tests.[6]

Test 16 was also coarsened at 2300° F and subsequently tested in the as-welded condition. Although Test 16 reached a total plastic deformation of 0.173 in. before conclusion of the test, an almost continuous network of fine cracks appeared in the heat-affected zone on each side of the weld after the first heating cycle. Test 17 having been given a 1900° F postweld heat treatment, was fluid-penetrant tested and carefully examined for cracks in the heat-affected zone. The results of the fluid penetrant examination on the outside surface of the test weld revealed numerous cracks in the heat-affected zone on each side of the weld as shown in Fig. 27. One of several sections removed from this test weld is shown in Fig. 28, and the results of a penetrant test on the OD surface and cross section through the pipe wall of this specimen is shown in Fig. 29. The cracks as shown on the outside pipe surface adjacent to the weld deposit are shown in Fig. 30 at 40 diameters magnification.

The 347 base metal for Tests 21 and 22 was coarsened at 2375° F. After completing the two test welds and rough machining, a number of fine cracks were noted to the eye. The tests were subsequently smooth machined and fluid penetrant tested. Numerous cracks were noted in the reduced section of the specimen, but a continuous network of cracks was noted at the extremity of the specimens at which location the specimen diam more nearly approached that of the bar as it was coarsened during heat treatment. A transverse slice was removed from one end of Test 22 and

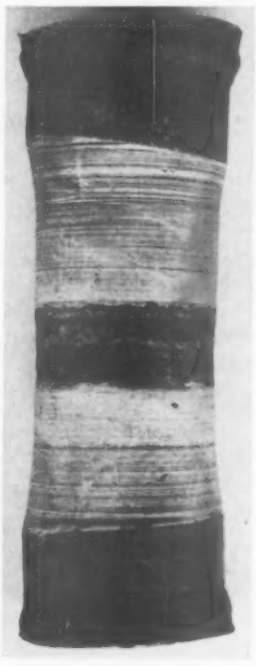


Fig. 25 Test 24 after removing flanged ends showing 16-8-2 weld-metal ductility (elongated thermocouple hole) and fusion line failure. X 1. (Reduced by 1/2 upon reproduction)

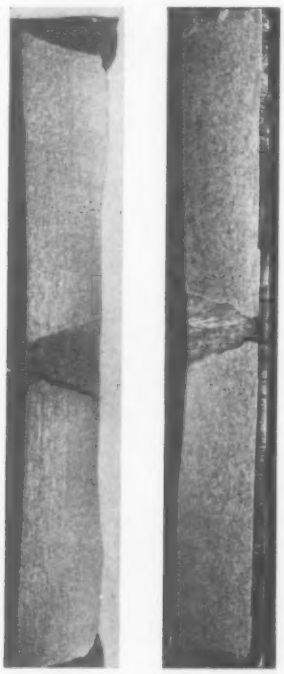


Fig. 26 Etched sections through pipe wall of test 24 (*left*) and Test 28 (*right*) indicating fusion line failure from the ID toward the OD. X 1. (Reduced by 1/2 upon reproduction)

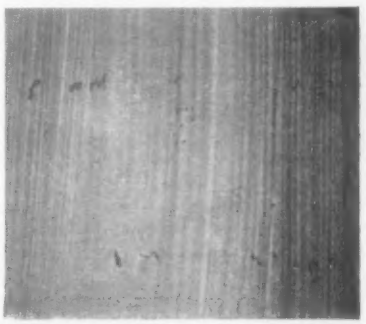


Fig. 27 Fluid penetrant examination of test weld 17 revealing many weld heat-affected zone microfissures before testing. X 1 1/2. (Reduced by 1/3 upon reproduction)

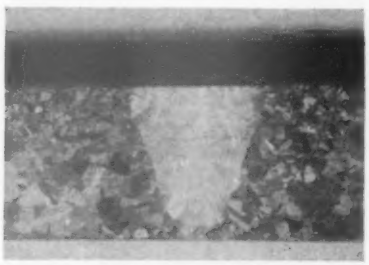


Fig. 28 Etched macrosection through pipe wall of Test 17. Etchant, electrolytic 10% oxalic acid. X 1.

polished. The surface of this test, including the cross section, is shown in Fig. 31, after fluid penetrant testing. It is believed that the temperature to which this specimen was heated resulted in liquation of the grain boundaries and that shrinkage stresses which developed upon quenching the specimen resulted in grain boundary ruptures. In view of the condition of this specimen it was not postweld heat treated as was originally scheduled.

The section shown in Fig. 31 is also shown in Fig. 32 after polishing and etching. Photomicrographs taken from Test 21 revealing base-metal rupture cracks resulting from the 2375° F coarsening heat treatment are presented in Figs. 33 and 34, the rupture crack extending into the crack-sensitive 347 weld deposit in Fig. 33. The intergranular ruptures noted in Tests 21 and 22 after the coarsening treatment are believed to correlate with the sharp drop in ductility observed in 347 material when tested above 2300° F in the Nippes test.[7] It is believed that the sharp drop in ductility occurs as a result of liquation or quasiliquation of the grain boundaries. The grain boundary phase in 347 is furthermore believed to be nonductile and upon solidification forms a network of microfissures which result in reduced ductility of specimens heated to the quasiliquidus condition and subsequently fractured at lower temperatures.

A re-examination of the 2300° F coarsened 347 tensile bar shown in Fig. 6, in light of the results of Tests 21 and 22, indicate the loss in ductility of this specimen as plotted in Fig. 5 to be no doubt due to the existence of microfissures in this specimen as a result of the coarsening heat treatment. The continued rise in ductility of the Type 316 material, plotted in Fig. 5, when coarsened at 2300° F is also in agreement with results of hot ductility tests made and recently presented on Type 316 material as compared with 347.[8]

Specimens Exhibiting No Failure

Nine of the 28 tests were concluded without failure. Of these, Tests 27, 12 and a macrosection of Test 23 are shown in Figs. 35, 36 and 37. These tests were concluded after absorbing 0.200 in. of plastic strain in 20 in. While a major percentage of this strain was taken through the center 5 in. of the test section, measurements of the distribution of strain over the 20-in. specimen length were not made.

Guided-Bend Specimens Removed from Tubular Tests

Guided-bend specimens were removed from several of the tubular specimens to determine the ductility across the welded joint after completion of high-temperature testing. Five tests (2, 4, 6, 10 and 27) were chosen which were welded with 347 electrodes, five (9, 12, 14, 11 and 15) welded with 316 electrodes and one (23) welded with 16-8-2 electrodes. Although three of the five tests welded with

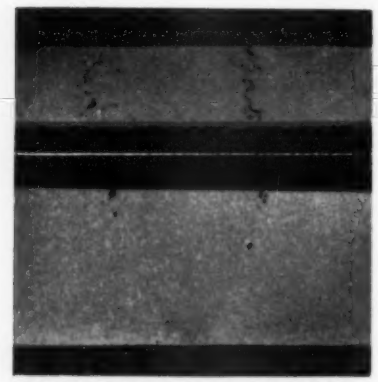


Fig. 29 Fluid penetrant test of OD surface (*top*), and cross section through wall (*bottom*) of specimen shown in Fig. 28. X 1

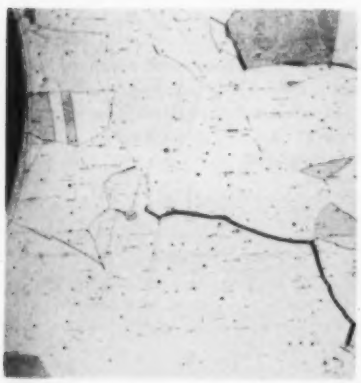


Fig. 30 Intergranular cracks in weld heat-affected zone of coarse grain 316 prior to elevated-temperature testing. Etchant, electrolytic 10% oxalic acid. X 40. (Reduced by $^1/_2$ upon reproduction)

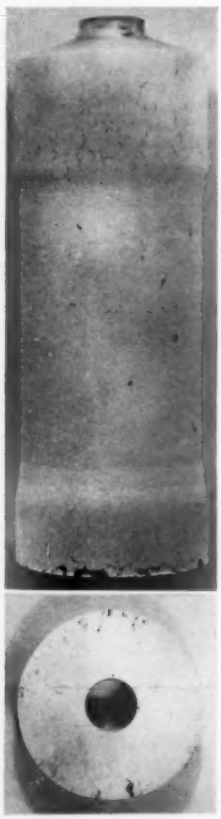


Fig. 31 Fluid penetrant examination of OD surface and transverse cross section of Test 22 revealing specimen damage as a result of coarsening heat treatment. X 1. (Reduced by $^1/_2$ upon reproduction)

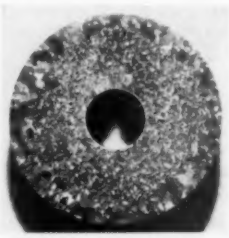


Fig. 32 Etched transverse cross section of specimens shown in Fig. 31. Etchant, electrolytic 10% oxalic acid. X 1. (Reduced by $^1/_2$ upon reproduction)

Fig. 33 Intergranular crack in weld heat-affected zone of coarse grain 347 extending into weld deposit. Etchant, Vilella's reagent. X 40. (Reduced by $^1/_2$ upon reproduction)

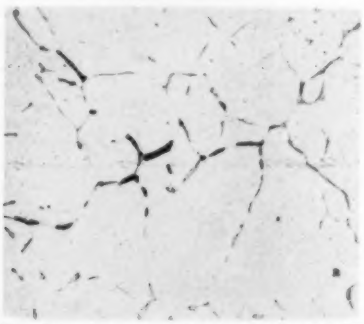


Fig. 34 Intergranular fissuring of 347 base metal resulting from high-temperature coarsening heat treatment. Etchant, Vilella's reagent. X 40. (Reduced by $^1/_2$ upon reproduction)

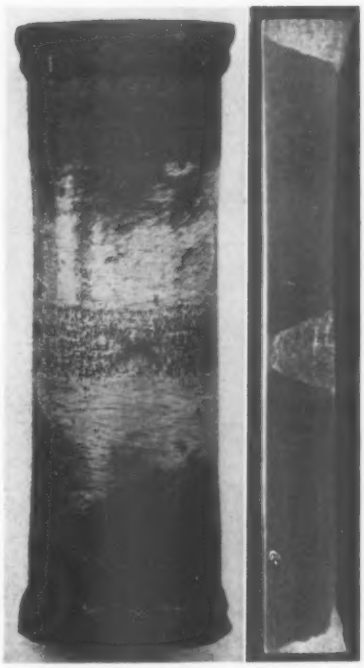


Fig. 35 Type 347 test joint postweld heat treated at 1900° F. (Test 27) with plastic strain of 0.201 in. without failure. X 1. (Reduced by $^1/_2$ upon reproduction)

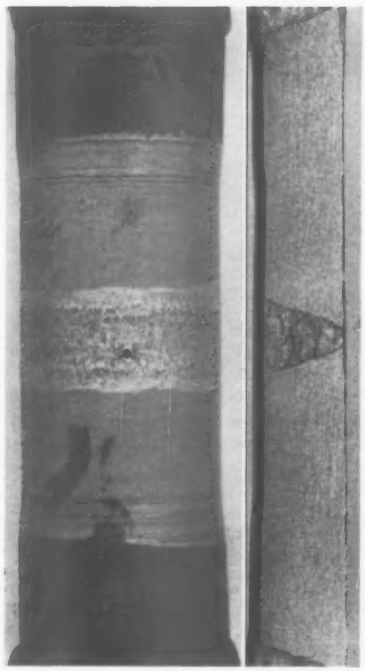


Fig. 36 Type 316 test joint in as-welded condition (Test 12) with plastic strain of 0.221 in. without failure. X 1. (Reduced by $^1/_2$ upon reproduction)

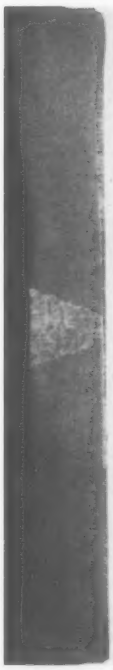


Fig. 37 Etched section through pipe wall of Test 23 showing no failure after absorbing a plastic strain of 0.217 in. X 1. (Reduced by $^1/_2$ upon reproduction)

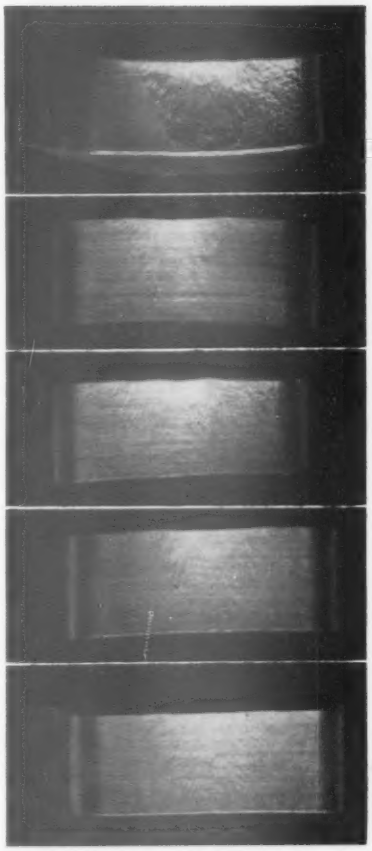


Fig. 38 Results of bend tests after elevated-temperature testing of tests (*from top to bottom*) 2, 4, 6, 10 and 27. (Reduced by $^1/_2$ upon reproduction)

347 failed in the heat-affected zone of the base metal during high-temperature testing, it is to be noted from Fig. 38 that all joints had excellent ductility when tested cold.

The five tests welded with 316 electrodes likewise had good ductility, although numerous small tears may be noted in the weld deposits after bending, as shown in Fig. 39. This condition is characteristic of fully austenitic electrodes and not necessarily the result of the previous high-temperature testing employed. Similar results have been found in the testing of fully austenitic 347 weld deposits when tested in the as-welded condition.

The results of the bend test using 16-8-2 electrodes and 316 base metal are also shown in Fig. 39. This test has excellent ductility and exhibits the same freedom from microfissuring as do the 347 deposits. As previously pointed out, however, the 16-8-2 deposit was found to be slightly magnetic as were the 347 deposits. The 16-8-2 weld metal, however, has been shown to be less sensitive to aging embrittlement than deposits of 347, particularly in the as-welded condition.[9]

Examples of Service Failures

Shown in Figs. 40 and 41 are examples of stress-rupture service failures which have been found to occur adjacent to welds similar to those found in simulated high-temperature service tests on laboratory specimens as previously illustrated.

Mechanism of Service Failures

Considerable information has been presented in the past year regarding the reasons for service failures in 347 base metal adjacent to welded joints.[4, 9, 10] The reasons are believed to be sound and are in good agreement with the results of this investigation.

Since the failures have been previously indicated as stress-rupture failures, the predominant factors responsible for these failures are believed to be high levels of combined stress, and the inability of the material to absorb the imposed stress by plastic deformation at the operating temperature. For the benefit of those who have not had the experience of welding girth seams in heavy-wall austenitic piping, it is to be pointed out that shrinkage as great as $^3/_{16}$ in. in diam, and $^5/_{16}$ in. longitudinally (transverse to the weld) is not uncommon. Obviously plastic strain of this magnitude results in high residual stresses in an as-welded joint of this type. References previously cited have shown peak residual stresses in cricular patch weld tests as high as 70,000 psi[10] with values of 25,000 to 35,000 psi quite common in heavy-wall pipe joints in the as-welded condition.

When joints of this type are placed in service in the as-welded condition, bending stresses due to thermal expansion, including the possibility of high localized

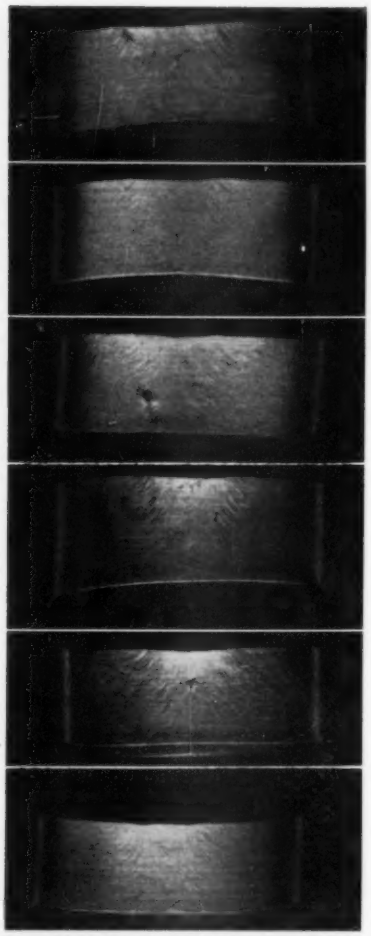


Fig. 39 Results of bend tests after elevated-temperature testing of Test (*from top to bottom*) 9, 12, 14, 11, 15 and 23. X 1

stresses due to mechanical or metallurgical notches, resulting from arc strikes, undercut, improper feathering of heavy cover beads, etc., longitudinal stresses due to internal pressure and thermal stresses due to temperature differential across the pipe wall will be algebraically added to the residual stresses.

If it were possible to accurately determine these additive stresses, it is believed that the length of time required to produce failure could be, for a given operating temperature, quite readily approximated by reference to the appropriate stress-rupture curve. With further reference to the mechanism of failure in 347 in particular, it has been pointed out that this material, whether it be wrought or cast weld metal, has good high-temperature strength, but poor high-temperature rupture ductility, the hot ductility of wrought material decreasing with increasing grain size.[2, 9, 10]

Since 347 maintains high strength at operating temperatures of 1050–1200° F

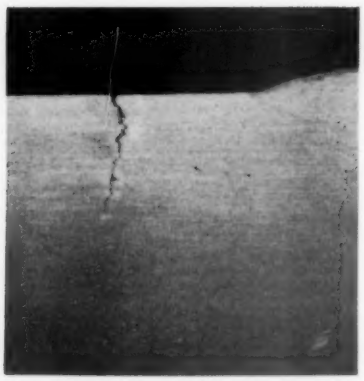


Fig. 40 Intergranular stress-rupture crack in $2^1/_4$-in. OD x $^5/_{16}$-in. wall Type 321 superheater tube adjacent to weld. X 9. Metal temperature during service 1100–1150° F

and cannot yield or plastically deform to relieve localized areas of high stress without failure due to low rupture ductility, failure occurs at the OD of the coarsened base metal adjacent to the line of fusion. The base metal at this location has been coarsened by the heat of welding, has the lowest hot ductility and has a high residual stress level.

The level of residual stress in an as-welded joint of a given geometry depends on the relative yield strengths of the weld deposit and base metal comprising the joint and the ability of one or the other to absorb plastic strain during the thermal cycles of welding.

The criterion for establishing the allowable level of residual stress in a welded joint to be placed in service should be at least in part based on the hot ductility characteristics of the material.

Materials having poor hot rupture ductility should of course, be placed in service with the minimum of residual stress, whereas material having good rupture ductility could be placed in service with higher residual stress levels since these materials have the ability to plastically deform at service temperatures and thus relieve the stress without rupture.

In addition to high residual weld stresses, bending stresses due to thermal expansion are believed to play an important role in the failure of welded joints in 347 material. If the piping system is not made sufficiently flexible, it is possible to develop very high bending stresses. Such stresses superimposed on high localized residual stresses at weld locations are believed to be fundamentally responsible for stress-rupture failures in 347 at the locations of welds.

Although residual weld stresses and bending stresses are believed to be the important considerations, longitudinal stress, due to internal pressure, and thermal stress, due to differential tem-

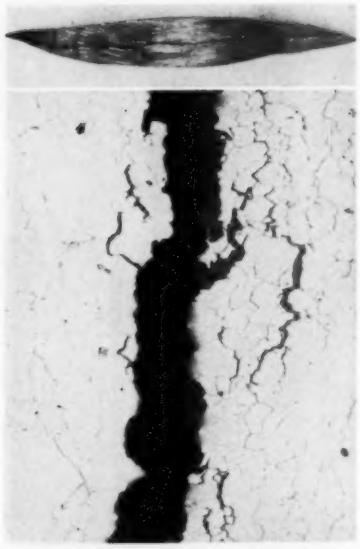


Fig. 41 Section removed from $10^3/_4$-in. OD x 1.5-in. wall Type 347 piping which failed in service (*upper*) and detail of intergranular fracture (*lower*). Steam service temperature 1050° F. (Reduced by $^1/_2$ upon reproduction)

perature across the pipe wall, are additional stresses which are present and cannot be avoided. These stresses are believed to be small under normal circumstances, although the latter stress could become large should a cold piping system be brought to operating temperature too rapidly.

Several examples of the effect of combinations of different levels of the various types of stress discussed, and their relative effect on service life before failure, are shown in Fig. 42.

Conclusions

1. Weld-heat-affected-zone service failures in 347 piping which occur at the OD and progress toward the ID were duplicated by simulated high-temperature service testing of welded tubular specimens.

2. Increasing the solution treating temperature and, therefore, the grain size of both 347 and 316 resulted in a general decrease of the room-temperature tensile strength, yield strength and Brinell hardness, and an increase in percent elongation and percent reduction of area.

3. A loss in room-temperature ductility was noted to occur in 347 when heated to 2300° F as compared to 2100° F. This loss in ductility was not observed in type 316 material. This leads to the thought that 347 may be more susceptible to structural damage than 316 when subjected to high temperatures including those from welding.

4. A slight increase in the room-temperature yield strength was noted in 316 when solution treated at 2100° F as compared with 1900 and 2300° F.

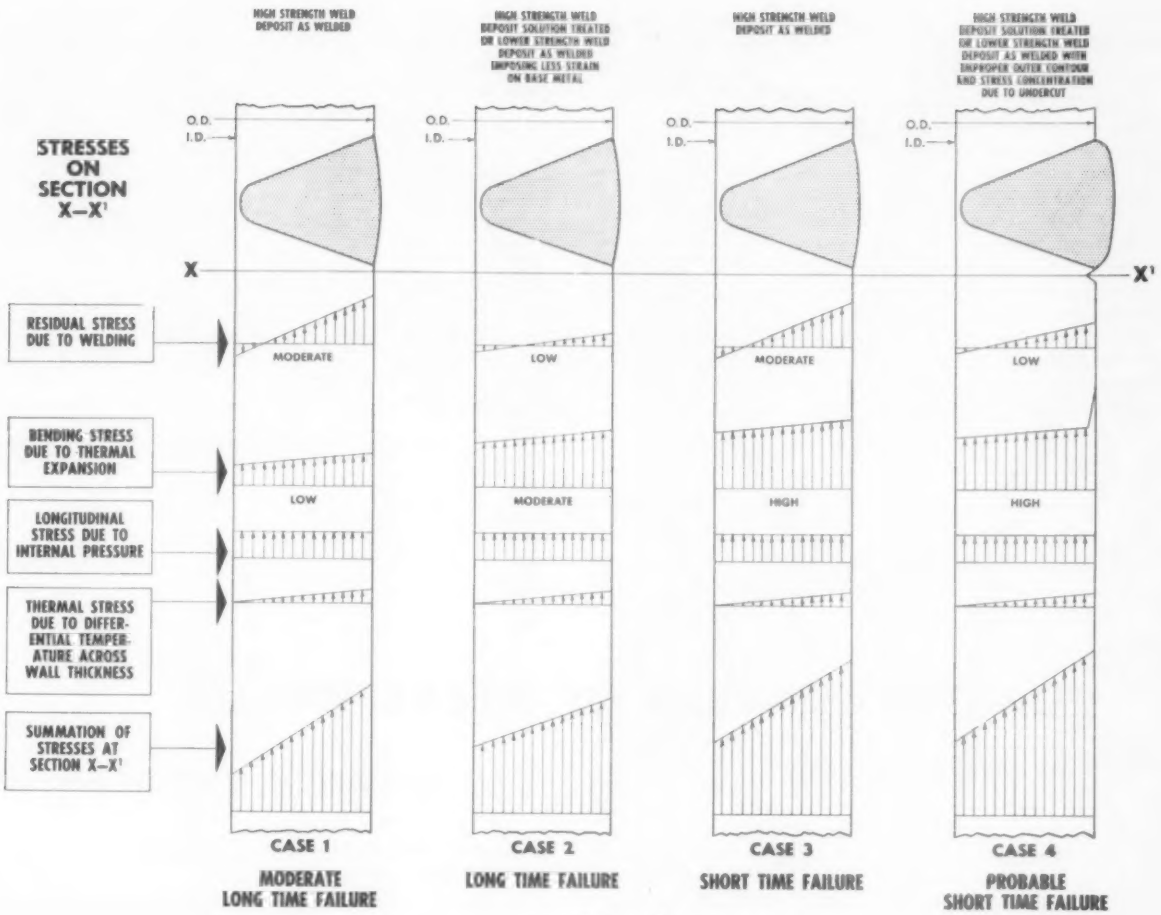

HIGH STRENGTH WELD DEPOSIT AS WELDED
HIGH STRENGTH WELD DEPOSIT SOLUTION TREATED OR LOWER STRENGTH WELD DEPOSIT AS WELDED IMPOSING LESS STRAIN ON BASE METAL
HIGH STRENGTH WELD DEPOSIT AS WELDED
HIGH STRENGTH WELD DEPOSIT SOLUTION TREATED OR LOWER STRENGTH WELD DEPOSIT AS WELDED WITH IMPROPER OUTER CONTOUR AND STRESS CONCENTRATION DUE TO UNDERCUT
STRESSES ON SECTION X–X'
O.D.
I.D.
X
X'
RESIDUAL STRESS DUE TO WELDING
MODERATE
LOW
MODERATE
LOW
BENDING STRESS DUE TO THERMAL EXPANSION
LOW
MODERATE
HIGH
HIGH
LONGITUDINAL STRESS DUE TO INTERNAL PRESSURE
THERMAL STRESS DUE TO DIFFERENTIAL TEMPERATURE ACROSS WALL THICKNESS
SUMMATION OF STRESSES AT SECTION X–X'
CASE 1
CASE 2
CASE 3
CASE 4
MODERATE LONG TIME FAILURE
LONG TIME FAILURE
SHORT TIME FAILURE
PROBABLE SHORT TIME FAILURE

Figure 42

5. The 1200° F yield strength of 347 tubular specimens containing as-welded joints was found to be lowered as the solution treating temperature was increased.

6. The 1200° F yield strength of 347 tubular specimens containing welds which were postweld heat treated at 1900° F was found to be 5000–8000 psi lower than the as-welded specimens.

7. The plastic ductility of as-welded tubular 347 specimens was found to be less than 0.1 in. in 20 in. with ductility decreasing as the base-metal grain size increased.

8. The plastic ductility of welded 347 tubular specimens given a 1900° F postweld heat treatment was found to be at a level of 0.20 in. in 20 in., or double that of the as-welded specimens.

9. The 1200° F yield strength of as-welded tubular 316 specimens was found to be considerably lower than the values observed for 347, the shape of the curve as a function of base-metal heat treatment, following closely that obtained on specimens tested at room temperature.

10. The 1200° F yield strength of 316 tubular specimens containing welds which were postweld heat treated at 1900° F was found, as in the case of 347 specimens, to be lower than the as-welded specimens.

11. The plastic ductility of welded 316 tubular specimens given a postweld heat treatment of 1900° F was found to be slightly greater than that for corresponding specimens in the as-welded condition. The as-welded 316 specimens, however, were found to exhibit 1200° F ductility at least equal to that obtained in 347 specimens given a postweld heat treatment.

12. Tack welds and arc-strikes giving rise to microfissuring (metallurgical notches) as well as sharp changes in cross sections, such as short radius fillets, undercut, improper blending of heavy cover passes into the adjacent base metal (mechanical notches) giving rise to stress concentration, may readily lead to premature stress-rupture failures in austenitic materials and particularly in the 347 analysis.

13. Tubular specimens of both 347 and 316 were welded with 16-8-2 electrodes and tested in the as-welded condition. The plastic ductility obtained using 316 base metal was found to exceed 0.20 in. in 20 in. and, based on these tests, therefore, indicated satisfactory performance. Duplicate tests made using 347 base metal, however, resulted in failure from the ID toward the OD along the line of fusion. Although the results were totally unexpected, it is believed that the results warrant additional study.

14. Base-metal fissuring adjacent to welds was found in both 347 and 316 prior to high-temperature testing. Results of these tests indicate that the grain-boundary liquation theory is applicable. The results indicate that 347 is much more sensitive to fissuring in the temperature range of 2300–2450° F than is 316 material.

15. Standard guided-bend tests removed from both 347 and 316 tubular specimens after high-temperature testing were found to have adequate room-temperature ductility.

16. A mechanism of failure is presented which is based on the summation of all stresses acting upon a given area at a given temperature for a given time. For a given operating temperature, stress rupture failures will occur with decreasing time as a function of increased stress applied. Localized areas of high stress (regardless of the origin of the stress) will fail prematurely, if the material has

no capacity to absorb this stress by plastic deformation.

Acknowledgments

The authors are indebted to William Gilson and Robert Simms, of the Pittsburgh Piping & Equipment Co.'s Metallurgical Staff, for the photographic and photomicrographic studies and to Ralph Crnkovic, Mechanical Department, Pittsburgh Piping & Equipment Co., for his assistance in performing and obtaining the data on the many high-temperature tests conducted.

The authors also wish to acknowledge the assistance given by Jack Succop, Heppenstall Co., who performed the high-temperature coarsening heat treatments on the bar stock used in this investigation. The 16-8-2 electrodes were furnished through the courtesy of O. R. Carpenter, Babcock & Wilcox Co.

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GRAPHITIZATION OF STEEL WELDS

BY JOSEPH G. WILSON

The Welding Research Council has issued Bulletin No. 32 summarizing the work of the American Petroleum Institute on the graphitization of steel in petroleum refining equipment.

The Bulletin is divided into two parts. The first report, entitled *Graphitization of Steel in Petroleum Refining Equipment*, presents the results of tests conducted on 554 specimens of carbon and carbon-molybdenum steel removed from petroleum refining equipment and examined for evidences of graphitization. Graphite formations were found in 33.8% of these specimens.

It has been established that some steels have a greater tendency to graphitize than others; and that high aluminum content (in excess of 0.015%), high operating temperatures and the steelmaking process used in meeting certain specifications are factors which affect graphitization. Of the steels examined, ASTM A 106 pipe indicated the highest tendency to graphitize; and, of the carbon steels, ASTM A 285 plate material indicated the highest resistance to graphitization. Connected nodules of graphite were found in both carbon and carbon-molybdenum steels. Although the carbon-molybdenum steel graphitized less frequently than the carbon steel, when it did graphitize, graphitization was of the more serious type.

The second report gives the results of studies of *The Effect of Graphitization of Steel on Stress-Rupture Properties.* It offers the following conclusions:

1. The most serious conditions encountered with regard to loss of rupture strength are the presence of defects in the welds and the presence of low-strength weld metal.

2. Although concentrated forms of graphite at weld heat-affected zones may adversely affect the rupture strength of steel, within the concentrations encountered in this investigation, other factors, such as weld quality, are of great importance. The data do not permit a definite statement relative to the effect of concentrated graphite on rupture ductility.

3. Random forms of graphite, even up to "moderate" degrees, in the unaffected base metal do not appear to be detrimental to either rupture strength or rupture ductility.

Copies of the Bulletin (44 pages) may be purchased at $1.00 per copy through the AMERICAN WELDING SOCIETY, 33 W. 39th St., New York 18, N. Y.

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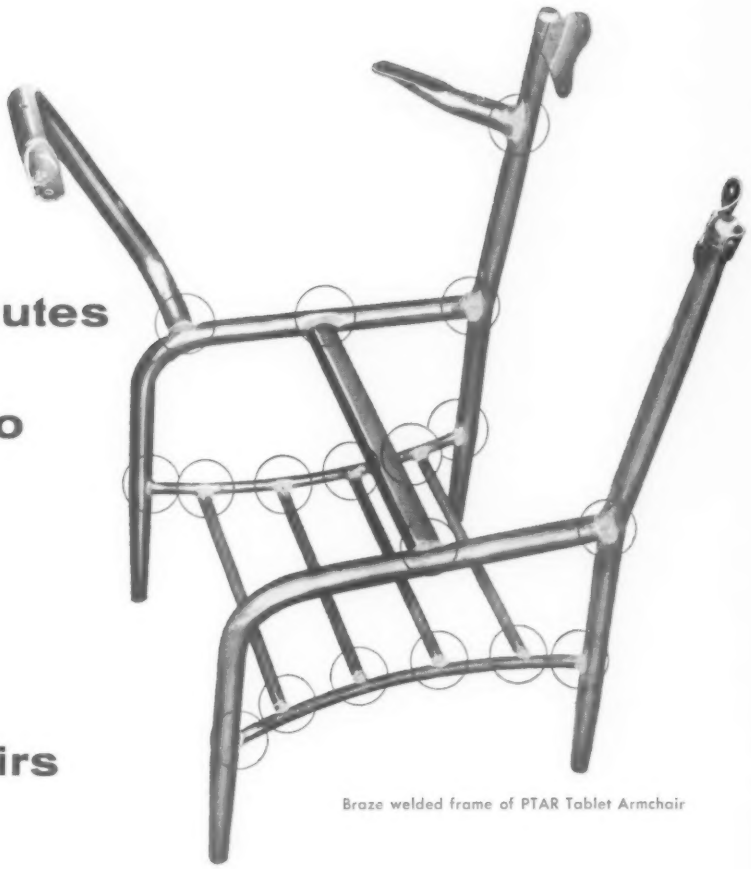

Braze welded frame of PTAR Tablet Armchair

Braze welded frames for Peabody school furniture—combination of chair and open-front table.

Anaconda-997 Low-Fuming Bronze Rods Make Well-Shaped Braze Welds, Essential for Eye Appeal and Extra Strength, Says Leading School Furniture Manufacturer—and Lower Production Costs too.

The Peabody Seating Company, Inc., North Manchester, Ind., has been making quality school furniture since 1902. Well-shaped braze welds, they say, are essential to maintain this quality. And they have found that Anaconda-997 Low-Fuming Bronze Rods make this kind of joint, not only adding eye appeal but providing the extra strength needed in school furniture.

By depositing smooth-flowing and low-fuming weld metal, these rods promote faster, more uniform work and thereby reduce production costs. The 18 braze welds pictured are made in 8 minutes—an average of about ½ minute per joint.

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